The WELDING JOURNAL

FEBRUARY 1952

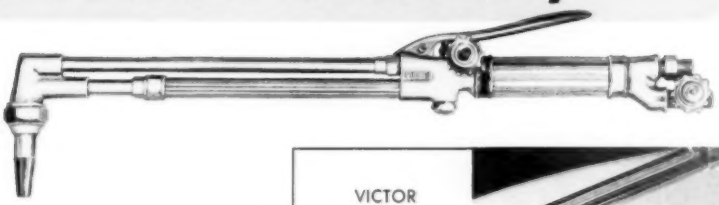
IN THIS ISSUE

Heavy Scrap Cutting in the Steel Mill 97
Thoriated-Tungsten Electrodes 102
Flash Welding of Components for Aircraft 109
Hard Facing for Impact 116
The Ship Structure Committee 65-s
Welded Reinforcement of Openings 67-s
Stress Studies of Welded Bulkhead Intersections 68-s
Ferrite-Grain-Size Measurements for Ship Steel 78-s
Fracturing and Fracture Dynamics 95-s
Testing the Cohesive Strength and Weldability of Steels 105-s
Table of Contents 95
Index to Advertisers 154

OFFICIAL PUBLICATION OF THE AMERICAN WELDING SOCIETY

the man on the job...

...Wants VICTOR Reliability



He gets reliability and efficiency in VICTOR welding and cutting torches, and regulators because they're built right.

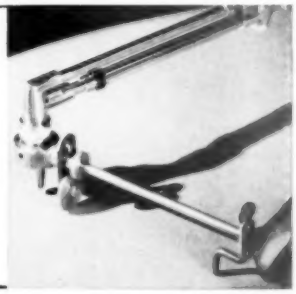
Precision machining assures leak-proof joints and ease of maintenance. Four head angles—90°, 75°, 45° or straight—and 4 lever positions—top or bottom and forward or rear of handle—enable you to choose the torch that exactly fits the job in hand.

VICTOR's complete range of tips, in sizes 000 through 16, are designed to give maximum cutting speed and gas savings on any job from light sheet to heavy plate cutting.

See for yourself why it costs less to own and operate VICTOR. Ask your VICTOR dealer for an on-the-job demonstration *TODAY*.

VICTOR
CIRCLE CUTTING
ATTACHMENT

adjusts to
various heights
and diameters
from 1¼" to 28".



VICTOR

Welding and Cutting Equipment Since 1910

VICTOR EQUIPMENT COMPANY

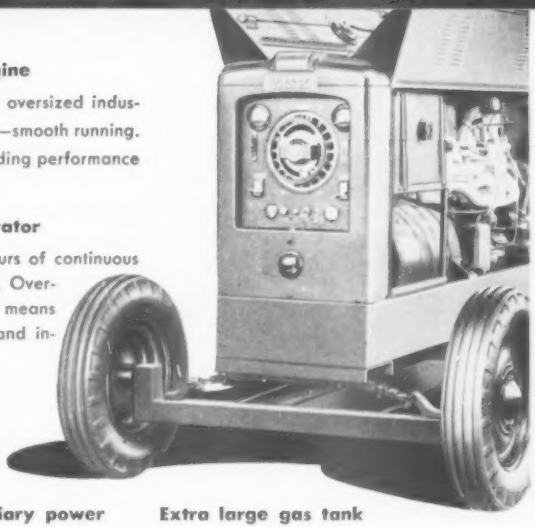
3821 Santa Fe Ave.	844 Folsom Street	1312 W. Lake St.
LOS ANGELES 58	SAN FRANCISCO 7	CHICAGO 7

Counterbalanced engine

Rugged, economical oversized industrial engine. Snappy—smooth running. Gives wonderful welding performance . . . all weather.

Extra capacity generator

Guarantees long hours of continuous heavy duty welding. Oversize 4 pole exciter means quick arc recovery and instant arc.



1 KW 110 volt auxiliary power

Supplies power for operating lights, tools, etc., where normal power is not available.

Automatic idling device

Saves fuel, $$$, and reduces wear by slowing down engine speed when arc is not being used.

Extra large gas tank

25 gal. capacity. Lets you weld for days without refilling. Saves plenty on every job.

Multi-range dual control

Gives operator choice of 1,000 combinations of welding heat.

WELDERS

Cool running

Air flows through, as well as around, the armature parts. Changes air twice as often as in ordinary construction.

Exceptionally stable arc

Reactance automatically adjusts to meet requirements of each welding range. Assures better, faster welds under all conditions.

Polarity control switch

Instant and positive. Makes it fast and simple to change electrodes and polarity.

No dead spots

. . . in the entire welding range—very important. Operator has choice of 1,000 volt-ampere combinations.

HOBART BROTHERS COMPANY
Box WJ-22, TROY, OHIO, U.S.A.

TEAR OFF AND MAIL TODAY

HOBART BROTHERS COMPANY, BOX WJ-22, TROY, OHIO

Without obligation, send latest catalog and full information on items checked.

- ☐ Gas Drive Welders
- ☐ Hobart Electrodes
- ☐ Electric Drive Welders
- ☐ Hobart Accessories

NAME

FIRM

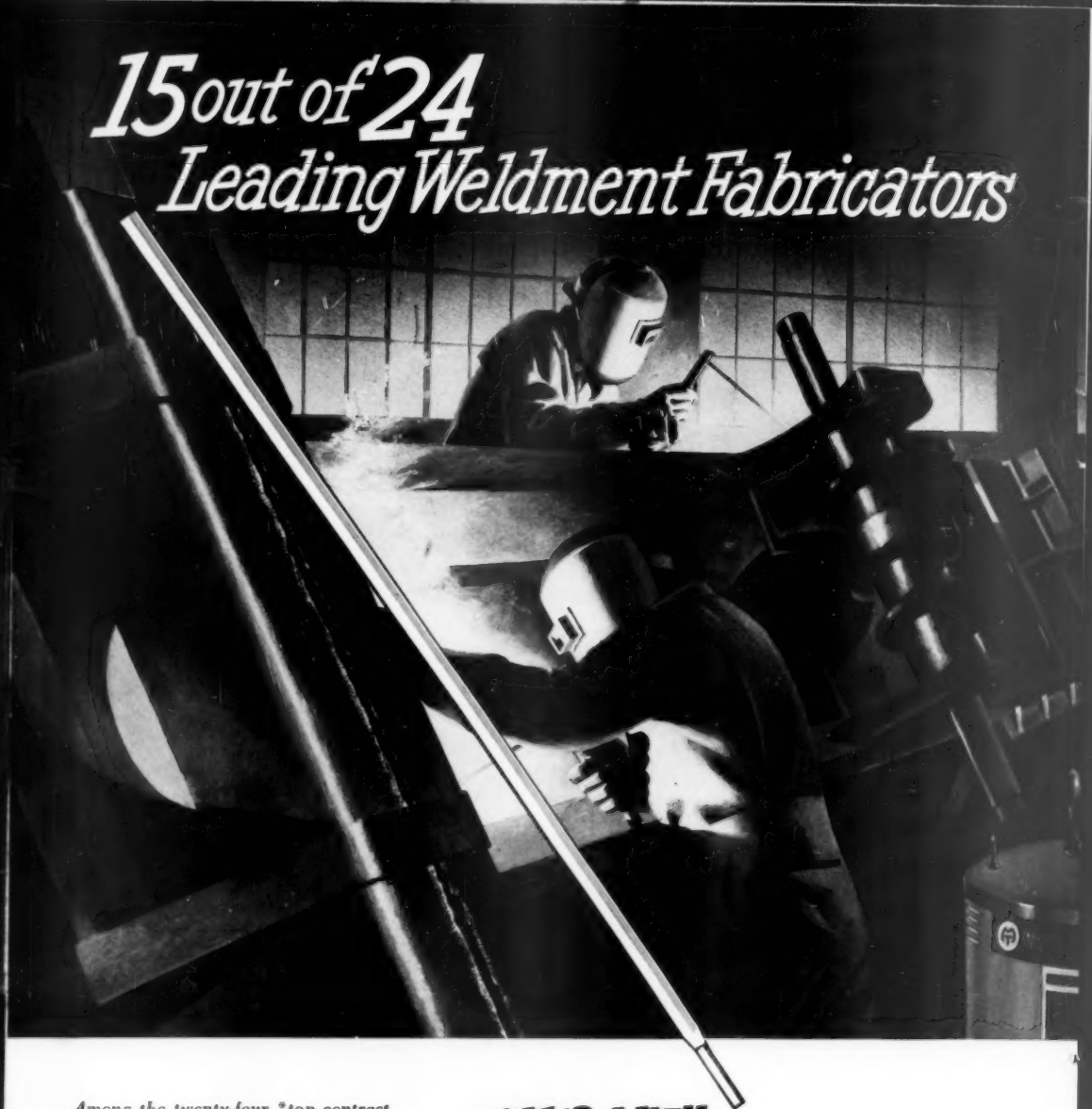
ADDRESS

FREE "Welder's Vest Pocket Guide to Better Welds" if you tell us work you're doing.

TROY, OHIO • "ONE OF THE WORLD'S LARGEST MANUFACTURERS OF ARC WELDERS" • BOX WJ-22

HOBART

15 out of 24 Leading Weldment Fabricators



*Among the twenty-four *top contract welding firms, fifteen regularly weld with Murex Electrodes.*

More of the leaders in important industries prefer Murex Electrodes in their welding operations because they can be sure of sound welding—high deposition rates for economy and speed of production.

*These having AAAA directory ratings.

WELD WITH...

MUREX®

METAL & THERMIT CORPORATION 100 EAST 42nd ST., NEW YORK 17, N. Y.

ELECTRODES • ARC WELDERS • ACCESSORIES

Value Beyond Expectation!

Mallory "Nu-Wrinkle Cap"* Electrodes

(cross-section of fluted water hole)

Conserve Copper Alloy... Cut Resistance Welding Costs

The Mallory "Nu-Wrinkle Cap" Electrode is composed of a replaceable cap inserted into a reusable shank. When the face is no longer serviceable, it is necessary only for the operator to replace the relatively small and inexpensive cap.

Extensive spot welding tests have shown that, in most applications, the performance of Mallory "Nu-Wrinkle Cap" Electrodes compares favorably with that of conventional one-piece electrodes, resulting in a considerable reduction of electrode costs and saving of critical copper alloy.

Caps are available in all standard Mallory nose types, in Elkaloy A† or Mallory 3† metals, with #1 or #2 Morse Taper shanks. All shanks have fluted** water holes, increasing the cooling surface area by 70% compared with round holes.

In addition, Mallory "Nu-Wrinkle Cap" Electrodes make readily replaceable inserts for die applications.

That's value beyond expectation!

Get complete information now. Write direct or contact your local Mallory Distributor.

In Canada, made and sold by Johnson Matthey and Mallory, Ltd., 110 Industry St., Toronto 15, Ontario.

Outstanding Design Features of Mallory "Nu-Wrinkle Cap" Electrodes

1. Smooth, annular corrugations of cap skirt provide positive electrical and thermal contact with shank.
2. Resilience of corrugations effects secure attachment of cap to shank.
3. Wiping action of corrugations cleans adjoining surfaces of shank, assures low electrical contact resistance.
4. Hollow cap skirt has the largest practicable cooling area, results in a maximum rate of heat transfer.
5. Smoothly rounded skirt corrugations minimize damage to shank, insure maximum shank life.
6. Unique design eliminates possibility of cap being forced askew into shank.

*Trade Mark, Patent Pending
**Patent Pending
†Reg. U.S. Pat. Off.

Resistance Welding Electrodes, Holders, Dies, Rod and Bars, Castings, Forgings

P. R. MALLORY & CO. Inc.

MALLORY

SERVING INDUSTRY WITH

Electromechanical Products—Resistors • Switches • TV Tuners • Vibrators
Electrochemical Products—Capacitors • Rectifiers • Mercury Dry Batteries
Metallurgical Products—Contacts • Special Metals • Welding Materials

P. R. MALLORY & CO., INC., INDIANAPOLIS 6, INDIANA

For information on Titanium Developments contact Mallory-Sharon Titanium Corp., Niles, Ohio

ANYONE

who can point this "gun"

can make a

GOOD SPOT WELD

"HELIARC" HW-8 Pistol-Grip Torch needs no forging pressure...works from one side of sheet... spot welds both STAINLESS and CARBON STEELS

Are you using light gage metals to fabricate large assemblies or irregular shapes? If so, chances are you can simplify many of your joining problems, boost production, and cut costs, too, by spot welding with the HELIARC HW-8 Torch.

The HW-8 joins mild steel, low alloy, or stainless steel .020 to .064 in. thick at one to two seconds per weld. Because it works *from one side of the sheet*, without forging pressure, it makes an easy, one-hand job of spot welding — even in places where resistance welding is not practical or possible.

Connected to a suitable power source with auxiliary timer, the HELIARC Spot Welding Torch makes inert gas shielded welds without fumes, smoke, or spatter. Since operation is automatically controlled, workmen on the assembly line need only press the "muzzle" of the "gun" against the work and pull the trigger. A single hose assembly permits free use of the torch over a 25-ft. radius.

For further information, telephone or write today. LINDE AIR PRODUCTS COMPANY, a Division of Union Carbide and Carbon Corporation, 30 East 42nd Street, New York 17, N. Y.

The term "Heliarc" is a registered trade-mark of Union Carbide and Carbon Corporation.

Linde Trade-Mark

Products and Processes for MAKING, CUTTING, JOINING, TREATING, and FORMING METALS

Published for the advancement of the science and art of welding by the AMERICAN WELDING SOCIETY

Editor
W. SPRARAGEN

The WELDING JOURNAL

Volume 31 **FEBRUARY 1952** *Number 2*

TECHNICAL PAPERS, ITEMS AND REPORTS

Heavy Scrap Cutting in the Steel Mill, by L. P. Elly ... 97
Thoriated-Tungsten Electrodes—Their Welding Characteristics and Applications, by G. J. Gibson and R. O. Seitz ... 102
Current Welding Literature ... 108
Flash Welding of Components for Aircraft and Similar Applications, by J. H. Cooper ... 109
Hard Facing for Impact, by Howard S. Avery ... 116
Discussion by C. E. Rogers ... 143
Discussion by Theodore Gaynor ... 144
Author's Reply ... 145

PRACTICAL WELDER AND DESIGNER

The Horton Multisphere, by Christian Arne ... 146
How to Braze Weld Cast Iron, by H. D. Drummond ... 148
Crusher Parts Made Tougher by Hard Facing Using Submerged Arc Welding ... 152

SOCIETY AND RELATED EVENTS

Society News ... 153
Index to Advertisers 154
Abstracts of Current Welding Patents ... 161
News of the Industry 164
New Products ... 168
Section News and Events ... 170
New Literature ... 181
Employment Service Bulletin ... 184
List of New Members ... 185
Personnel ... 186

WELDING RESEARCH SUPPLEMENT

The Ship Structure Committee, by Rear Admiral K. K. Cowart ... 65-s
Welded Reinforcement of Openings in Structural Steel Members, Discussion by Wiley E. Magee ... 67-s
Stress Studies of Bulkhead Intersections for Welded Tankers, by Wm. R. Campbell, L. K. Irwin and R. C. Duncan ... 68-s
Discussion by Captain Wendell P. Roop, USN (Retired) ... 75-s
Discussion by James B. Robertson, Jr. ... 76-s
Discussion by John Vasta ... 76-s
Authors' Reply ... 77-s
Ferrite-Grain-Size Measurements for Ship Plate Steel, by J. E. Campbell, R. H. Frazier and H. O. McIntire ... 78-s
Discussion by S. Epstein and W. P. Gerhart ... 90-s
Discussion by Stuart V. Arnold ... 92-s
Discussion by K. J. Stodden and R. W. Vanderbeck ... 92-s
Authors' Closure ... 94-s
Fracturing and Fracture Dynamics, by G. R. Irwin and J. A. Kies ... 95-s
A Study of the Fatigue Strength of Welded Joints, by W. Soete and R. Van Crombrugge ... 100-s
Fatigue Tests of Arc-Welded Joints, by H. de Leiris and H. Dutilleul ... 104-s
The Practical Testing of the Cohesive Strength and Weldability of Steels, by W. A. Felix ... 105-s
Announcement of Predoctoral Fellowships on the Fundamental Sciences Related to Welding ... 111-s

OFFICERS OF THE SOCIETY

C. H. JENNINGS	*President*
F. L. PLUMMER	*1st Vice-President*
E. R. SEABLOOM	*2nd Vice-President*
R. S. DONALD	*Treasurer*
J. G. MAGRATH	*Secretary*
F. J. MOONEY	*Asst. Secretary*
S. A. GREENBERG	*Technical Secretary*

DISTRICT VICE-PRESIDENTS

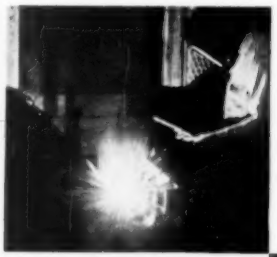
1—New York and New England	E. N. ADAMS
2—Mid-Eastern	H. L. INGRAM, JR.
3—Southern	J. U. DURHAM
4—Central	T. J. CRAWFORD
5—Mid-Western	T. B. JEFFERSON
6—Mid-Southern	A. E. WIBLER
7—Western	A. P. MARADUDIN

Published monthly by the American Welding Society. Publication office, 20th and Northampton Streets, Easton, Pa. Editorial and general offices, 33 West 39th St., New York 18, N. Y. Subscriptions $6.00 per year in United States and possessions; foreign countries $7.50. Single copies, nonmembers $1.00; members 75 cents. Entered as second-class matter January 5, 1932, at the Post Office at Easton, Pa., under the act of March 3, 1879. Copyright 1952, by the American Welding Society. The Society is not responsible for any statement made or opinion expressed in its publications. Permission is given to reprint any article after its date of publication provided proper credit is given.

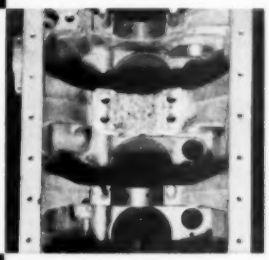
Don't scrap that casting

New Welding Technique
...salvages cast iron crank cases with scored bores

Scored or pinched bores used to mean the scrap pile for cast iron crank cases. But that's not so in service departments of Autocar Company branches since they installed a new salvage-by-welding technique. Here's how it's done —

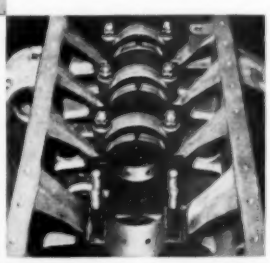


1 Welder deposits a layer of Ni-Rod "55" in center brace bore, while crank case bolted to block rests in upside-down position. This crank case bore hole had previously been machined down about .045" to remove scored area and eliminate oil-impregnated cast iron material. Fast work prevents store-up of excessive heat in cast iron brace.



2 Finished weld deposit has one-bead thickness. Using a ⅛" dia. Ni-Rod "55" electrode, the welder first deposited metal around the oil holes, then laid beads lengthwise in the bore, working from center outward and on alternating sides.

3 Now for the machining: The bearing cap is placed in a fixture. The joint surfaces are ground smooth and in parallel, then securely fastened in place. A special boring bar (bar held in perfect alignment with the other bores) and Carboloy-tipped cutting tool machine Ni-Rod "55" deposit down to the original dimensions. (Tolerance is held to 0.0005 in.)



4 As good as new. After machining, the bore's smooth new surface of weld metal is ready to receive the new bearing. Then the crank case is ready to be assembled. The first case salvaged this way has been in continuous service for over two years now, giving perfect service.

...you can save it with Ni-Rod®

There's one thing you know about increased defense production. As many broken or damaged castings as possible must be salvaged.

And with the proper techniques and equipment you can do just that.

With Ni-Rod and Ni-Rod "55", for example, you can make sound machinable welds in cast iron.

Welds that will restore equipment to essential production and at the same time save the cost of replacements.

And Ni-Rod is easy to use, too. Even a welder with limited experience can quickly learn to lay high-strength, sound, non-porous, machinable welds. Pre-heating or post-heating are seldom required.

For information on how to use Ni-Rod and Ni-Rod "55" for machinable welds in cast iron, write for your free copy of "Ni-Rod, an electrode for any cast iron welding."

And remember, defense production requires a lot of Nickel and Nickel alloys. Therefore, it may take longer to get the Ni-Rod you want. But, you can help speed delivery of your order by including the D.O. or C.M.P. rating. Consult your distributor on present availabilities: he will be glad to keep you posted. And you can call on him for technical information too.

THE INTERNATIONAL NICKEL COMPANY, INC.
67 Wall Street, New York 5, N. Y.

EMBLEM OF SERVICE
INCO

Write today for your free copy of "Ni-Rod, an electrode for any cast iron welding."

The WELDING JOURNAL

volume 31 February 1952 number 2

Heavy Scrap-Cutting in the Steel Mill

Application of oxy-fuel gas cutting increases the volume of heavy melting scrap delivered to the open-hearth furnaces and lowers the cost of scrap preparation

by L. P. Elly

THE tremendous demand on the steel industry for more and more ingot tonnage has presented a very severe demand for heavy melting scrap. Our organization, therefore, has been actively engaged in improving its scrap preparation practice and searching for new sources of heavy melting scrap in order to meet this demand.

A large portion of our heavy melting scrap is prepared for open-hearth consumption at our Scrap Drop, which is an open crane runway approximately 880 ft. long by 75 ft. wide. The runway is equipped with five overhead cranes, two rated at 30 ton and three at 40 ton capacity. The 30-ton units have single hoist trolleys and the 40-ton units have double hoist trolleys. They are equipped for bucket, electric magnet or chain lift. The runway is serviced by twelve railroad tracks, some transverse and some longitudinal. In the runway there are five pits for breaking scrap by drop balls, which vary from ten to eighteen tons in weight. Prior to our present burning practice, there was a 100 ft. by 75 ft. area used for storage, lancing holes for blasting, and blasting of open-hearth buttons, forging butts, steel-casting risers, mined scrap and other miscellaneous heavy scrap in order to reduce it to charging size. Scrap of this nature can be seen in Fig. 1.

Our continual efforts to deliver from this scrap drop a greater volume of heavy melting scrap to the open hearth furnaces at the lowest possible cost resulted in the oxy-fuel gas burning of heavy scrap which had formerly been blasted. The details of this transition comprise the subject matter of this paper.

The oft-used method of time study was employed as the initial step in the program. The complete cycle of storing, lancing holes for blasting, and blasting of heavy scrap was studied by time-study engineers under our direction. Analysis of their study revealed that two separate and distinct problems required solution.

Fig. 1 Open hearth steel buttons and scrap ingots mounted on transfer cars which run on a sub-level track transverse to the main scrap runway

According to the study, our existing practice under the scrap drop crane runway was inefficient because of (1) insufficient laydown area for the complete blasting operation and (2) the operation required an excessive amount of handling by overhead crane.

Heavy scrap to be blasted was received at the scrap drop in railroad cars from the open hearths, forge shops steel foundry and our scrap mining operation. It was then transferred to a storage pile adjacent to the lancing area by overhead crane. From here it was spread out in the burning area, where holes were lanced in each piece for blasting charges. The laydown area for hole lancing of 1000 sq. ft. permitted enough material storage for approximately two hours of lance time. The placing and removal of material for hole lancing by the chainman and dropman consumed considerable time due to the handling difficulties associated with the size

L. P. Elly is Welding Supervisor of the Bethlehem Steel Co., Bethlehem, Pa.

Paper was presented before the Thirty-Second Annual Meeting, A.W.S. Detroit, Mich., week of Oct. 15, 1951.

and shape of the scrap. The burners were nonproductive during this phase of the operation. After the holes were lanced, the scrap was then transferred to a storage pile adjacent to the blasting pits. Direct charge from the hole-lancing area to the blasting pits was not possible because of the residual heat from oxygen lancing. From this pile, scrap was moved to the blasting pits, holes were charged, pit covers installed and the charges detonated. Charging-size scrap was then placed in railroad cars by overhead crane for shipment to the open hearth. Frequently, however, scrap did not break as planned and had to be completely reprocessed.

There were several additional inherent disadvantages in the blasting process. The blasting was restricted to day and middle shift operation to eliminate disturbance to the community during sleeping hours. All men in the exposed area were required to take cover during each detonation, resulting in considerable nonproductive time. The inability to design blasting pits which would hold up for any appreciable time presented a maintenance expense of approximately $20,000 per year and extensive shutdown periods to make repairs.

The problem of insufficient laydown or work area and crane capacity underneath the drop was solved by the installation of a railroad track running at right angles to the crane runway and extending approximately 150 ft. beyond the runway. Four low-sided railroad cars operate on this transverse track. Scrap to be burned is placed on these cars, set on billets or beams to afford clearance between the scrap and the top of the car. The cars are protected from burning slag by a 1-ft. layer of sand, and accumulated slag and splatter are cleaned from the cars approximately once per week. After the cars are loaded with scrap under the crane, they are moved outside the runway by car winch for lancing. Details of the transverse track and car installation may be seen in Fig. 1. A working area adjacent to the cars is arranged by an earth fill to car top level on both sides of the track, as seen in Fig. 2. This installation provides sufficient laydown area for blasting hole preparation and burning of small scrap for an entire 8-hr. daylight operation. A far more desirable crane load distribution over the three shifts results.

The problem of developing a more economical severance practice than blasting, however, was more complex. Considerable interest in the use of oxy-fuel gas cutting of heavy scrap was current and investigation of its possible application to our problem was undertaken. In cooperation with a commercial vendor of oxy-fuel gas equipment, trial cuts on open-hearth buttons, forging butts, mined scrap and casting risers were made with portable oxy-acetylene equipment. The trial work clearly indicated that scrap of this nature, in sections from 30 to 60 in. in thickness, with its dirt, slag, refractory inclusions and surface scale, could be successfully severed by burning. The costs developed during the trial work also indicated that the practice, with proper setup and mechanization, was competitive and cheaper than our existent blasting practice. The initial trial work further indicated that available burning torch and tips were of insufficient capacity to cover the full range of section and density of the scrap to be burned. The desirability of eliminating the tip seating problem, aggravated by the extreme heat inherent in the operation, was evident.

Fig. 2 Oxygen lancing of holes for subsequent blasting of a large scrap ingot in order to reduce it to size for open hearth furnace charging

Thus, it was concluded that a mechanized cutting machine, equipped with a cutting torch and tip of sufficient capacity, should be set up for heavy scrap severance. Because much of this scrap required more than one cut for complete sizing, it was felt that the mechanized torch should be able to cut in any direction to

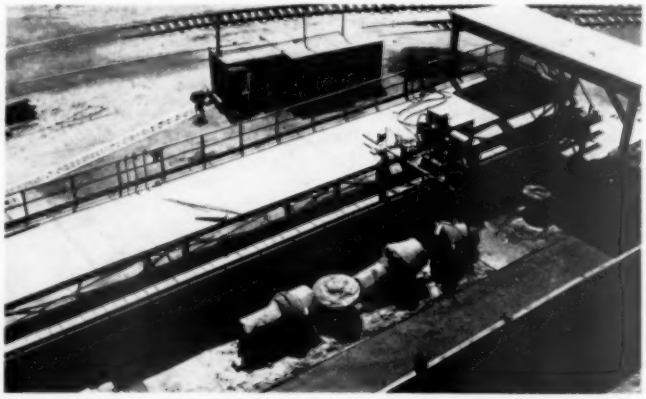


Fig. 3 An over-all view of the oxy-fuel gas burning machine installed to burn heavy scrap. The machine carriage rail, 80 ft. in length, and tracing table are parallel to the transverse rail line to permit burning outside of the scrap drop runway

Fig. 4 The cutting torch is being positioned for a new cut. Note that the unit is operated by one man from a push button panel mounted on the cutting machine bar

eliminate repositioning of scrap between cuts. It was further concluded that the machine should operate outside of the crane runway in conjunction with the existing transverse track heretofore described.

The cutting machine which was adapted to heavy scrap burning is a secondhand multiple torch cutting machine with associated rail and tracing table installed adjacent and parallel to the existing transverse railroad track and cars. An over-all view of the installation can be seen in Fig. 3. A 17-ft. long, 3-in. square bar is mounted to a pantograph arm arrangement which is carried by the main machine carriage. A tracing or driving mechanism is mounted on one end of the bar and develops its driving traction with the steel top tracing table. The mechanism drives and guides the cutting torch mounted on the other end of the bar. The pantograph mounting of the arm permits cutting in any direction. The main carriage runway is 80 ft. long, so that a burning area of 10 ft. in width by 80 ft. in length is accessible to the cutting torch. All controls are remote and located on a push button panel mounted on the burning machine bar. Thus, the burning machine is operated by one man, as shown in Fig. 4.

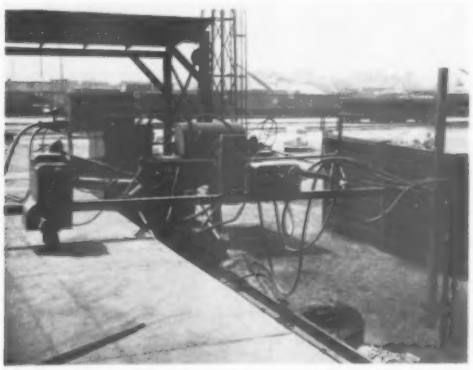


Fig. 5 A view showing the "business" end of the burning machine. From left to right, mounted on the machine bar, are the complete motorized driving unit, central push button control panel, remote elevator drive for vertical torch positioning, and the heavy capacity cutting torch

The bar driving unit is seen mounted on the left end of the machine bar in Fig. 5. This is a specially designed unit with steering, raising and lowering, and cutting drive mechanisms, all motorized and controlled at the centrally located push-button panel seen in this picture. The cutting drive is equipped with a "slip" type clutch to prevent motor overload in case of mechanical difficulties.

The driving unit is equipped with a 6-in. diameter, knurled, heat-treated steel driving wheel. The unit is geared for two speed ranges. The cutting speed can be varied between $^1/_2$ and 6 in. in the low-speed range and between 2 and 22 in. in the high-speed range. The entire weight of the unit, approximately 125 lb., rests on the tracing table, so that, despite the heavy load on the bar and the high winds sometimes present, the drive does not slip. The driving wheel is raised from the tracing table by motor to permit rapid positioning of the torch between cuts. Our initial experience indicated that the standard type drive associated with this type of cutting machine was not of sufficient capacity for the heavy loading and hence, a source of considerable downtime. The present drive has completely eliminated this loss of production.

The cutting torch is seen in Fig. 5, mounted on a vertical bar which operates through a slide box attached to the cutting machine bar. The vertical motion of the 3-in. square torch elevator bar is accomplished by a slide box with a motor-driven gear rack and pinion arrangement. The original drive mechanism was mounted directly on the slide box. However, because of the excessive weight on the extreme end of the machine bar, a remote drive was built and mounted adjacent to the push-button control panel, as seen in Fig. 5. A manually operated control is also incorporated in this unit, to be used in case of motor failure. The vertical torch motion range is approximately $6^1/_2$ ft. and the torch can be positioned vertically at a rate of 3 to 30 in. per minute. This permits vertical positioning of the torch during cutting to accommodate the varied shapes that are encountered. Further positioning of the torch is possible through its universal mounting to the vertical bar.

The cutting torch is of special design, incorporating a cutting oxygen passage and a separate preheat system mounted to the torch barrel. The preheat gases are mixed in a separate flash-resistant mixer and are then introduced to the cutting tip through an annular ring surrounding the top portion of the tip. Thus, the problem of maintaining tight seating of both preheat and cutting gases between the tip and torch head is eliminated. Therefore, no auxiliary cooling is required. The maximum cutting capacity of the torch has not been realized by us to date, but cuts of 60-in. sections have been made.

A range of four cutting tips is available for both

acetylene and propane gas. The cutting orifice diameters range from 0.5 to 0.8 in. An adaptor for this torch is available to permit the use of small capacity cutting tips on lighter work without changing torches.

The complete electrical and gas control equipment is mounted on the main machine carriage, as seen in the background of Fig. 5. A 1000-watt motor-generator set provides 220-v., 60-cycle current for all parts of the unit. The oxygen and fuel gas are supplied from pipeline service to the machine by hose and then regulated to operating pressures dictated by the nature of the scrap to be burned. Gases, supplied to the torch by hose, are controlled by solenoid valves located in the regulator box.

Oxygen is furnished to this operation by an underground pipe line, supplied from a bulk oxygen installation. Acetylene was used during the first year of operation with this burning machine. It was also furnished by underground pipe line, supplied from manifolded cylinder acetylene. After several months of successful operation, the possibility of generated acetylene with its obvious economies was explored. Because of the geographical location of our operation, power, compressed air, water and sewer facilities were not available. Study indicated that the installation of an acetylene generating unit would involve a very high capital investment. It was realized that a bulk supply of propane, however, could be set up in this location with a considerably lower installation cost. Therefore, oxygen-propane tips were developed for the torch and a trial run of several months from manifolded cylinder propane was instituted. Results with oxy-propane were as satisfactory as with acetylene and it was decided to operate the unit with propane. Installation of a bulk propane supply for this unit is now under way.

Gas pressures and cutting speeds used in burning scrap of this nature vary extensively. Surface scale or slag varies from a negligible amount to as much as 1 in. in thickness. Inclusions vary from a few to many; these include sand, refractory brick and miscellaneous other solids. Density of section varies from sound to very porous. Therefore, quoting of actual gas pressures and cutting speeds is extremely difficult and of little use. Sections to be cut vary from 30 in. upward to 60 in. in thickness, with a large volume of work falling in and around 45 in. in thickness. Cutting oxygen pressure for this range of work, as measured at the entrance of the torch barrel, runs between 10 and 30 psi. Acetylene pressure will average 10 psi. and propane pressure 20 psi. Cutting speeds will vary from 1 to 10 in. per minute, depending on section thickness, density and geometry of shape. With these pressures, cutting oxygen consumption, using the No. 2 tip, will vary from 6000 to 10,000 cu. ft. per hour (100% torch cutting time). The No. 2 tip will consume 395 cu. ft. per hour of acetylene, or 360 cu. ft. per hour of propane gas.

Cutting starts are accomplished easily and quickly. On some scrap where scale is particularly heavy, such as mined scrap, starts are facilitated by the injection of some molten steel on the starting surface of the scrap. The operator accomplishes this by introducing a small diameter steel pipe or rod into the preheat flames. Cutting tip maintenance is greatly minimized by maintaining a 2 to 6 in. distance between the tip exit and the work surface. This is possible on any geo-

Fig. 6 A view showing a transverse cut proceeding from right to left in a 35-in. forging butt section

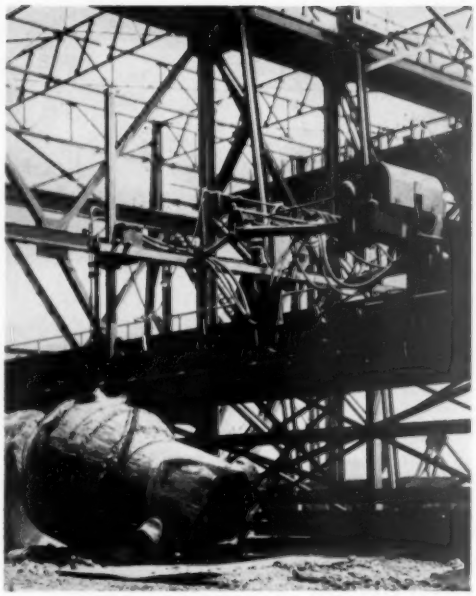


Fig. 7 A view showing a longitudinal cut proceeding from left to right in a 55-in.- ingot sinkhead section

metrical shape we encounter with the electrically driven, remotely controlled elevator drive.

As heretofore mentioned, our machine can burn scrap with cuts parallel to the railroad cars, transverse to the cars, or any combination of these two motions. Some of the larger open-hearth "buttons" are cut in three pieces, using only two cuts, thus requiring only one positioning of the "button." A transverse cut in a 35-in. section of a forging butt end can be seen progressing from right to left in Fig. 6. A longitudinal cut in a 55-in. section of an ingot sinkhead can be seen in Fig. 7.

Complete severance cuts with the exception of a small residium section resulting from "drag" can be realized. However, with any one section being burned, there is a break-even point which determines the degree of severance most economical to the operation. Generally, the machine operator will set his burning conditions to permit the most economical burning cost and still permit the scrap to be readily separated by the drop ball. Occasionally, the geometry of some scrap is such that more cuts in thinner sections, although involving additional handling, is more economical than few cuts in much heavier sections involving only one handling. These decisions are made by the foreman and his machine operator and, thanks to their experience, few pieces of scrap are returned to the machine for additional burning. Examples of some of the heavy cuts accomplished in mined open-hearth "buttons" can be seen in Fig. 8.

Fig. 8 A view of the exposed faces of cuts accomplished in mined open hearth steel "buttons"

Tonnage and cost information on a burning operation with so many inherent variables must be limited to average figures. Based on data accumulated during an eighteen-month period of operation, we averaged 8.69 tons of scrap prepared per machine operating-hour and thus per man-hour. This scrap preparation consumed an average of 625 cu. ft. of oxygen and 34 cu. ft. of fuel gas per ton of prepared scrap.

Economical accounting practice does not permit a breakdown of the cost of scrap prepared by burning from the over-all output of the entire scrap drop operation. However, comparing the cost per ton of the entire output of the operation, using the old blasting method and the new burning method, reveals a $0.20 reduction per ton since burning heavy scrap has replaced the blasting operation. Based on an average monthly total output of 23,000 tons, the estimated savings with burning instead of blasting develops an annual reduction of $55,200 in scrap preparation cost. Thus the cost of the burning installation, a major investment of approximately $25,000, has been returned to Management in less than six months.

In summarizing, we see that oxy-fuel gas cutting of heavy melting scrap has successfully replaced our previous practice of blasting. However, the scrap preparation problem must be thoroughly understood before the burning process is applied. Time study is most beneficial toward this end.

The development and application of the oxy-fuel gas cutting of heavy melting scrap, with its high degree of efficiency, are excellent examples of the benefits possible through the combined efforts of the steel mill and the commercial oxy-fuel gas manufacturer.

Oxy-fuel gas cutting of heavy melting scrap to open-hearth charging-size is accomplished economically. It results in a very high preparation rate per man-hour. This is the combination necessary to meet the tonnage demands currently placed upon the Steel Industry.

Thoriated-Tungsten Electrodes—Their Welding Characteristics and Applications

The theory of arc welding is examined to determine the difference between thoriated- and pure-tungsten electrodes

by G. J. Gibson and R. O. Seitz

INTRODUCTION

WHEN a welder experienced in inert-gas shielded-tungsten-arc welding first tries thoriated tungsten for straight polarity welding, several differences in operation are noticed. First, the striking of the arc is much smoother, touch starting can be accomplished with less tendency of the electrode to stick and with no noisy arc sputtering. Arcs at low current values can be started much more easily and with no tendency of the arc to climb up the electrode. If the arc is started with a high-frequency spark, the arc will start at longer arc lengths and at lower currents and will start instantly instead of having a delayed flash and sputter action.

A welder also notices a difference in the temperature color of the end of the electrode. The tip of a thoriated electrode will not be as brilliant, and the end will not be molten (See Fig. 1). As welding is continued there will be less tendency of the electrode to be contaminated by the molten weld metal when the electrode accidentally touches the workpiece and it will not be necessary to stop welding to grind or break off a length of the contaminated section. Thus, at the end of the day, more production will have been accomplished and much less electrode material will have been used than would have been the case with pure-tungsten electrodes. When a welder goes from job to job and finds it necessary to change his current values, it will not be necessary to change the electrode size as often as before. If a point is ground on the electrode the point will be maintained, and $^1/_8$-in. electrodes can be used effectively even for welding currents of 50 amp. or less.

The object of this investigation was to examine the theory of arc welding to determine the difference between thoriated- and pure-tungsten electrodes, and to run tests to determine the comparative values of these characteristics noted in the arc.

G. J. Gibson and **R. O. Seitz** are connected with the Air Reduction Laboratories, Murray Hill, N. J.

Paper presented at the Thirty-Second Annual Meeting, A.W.S., Detroit, Mich., week of Oct. 15, 1951.

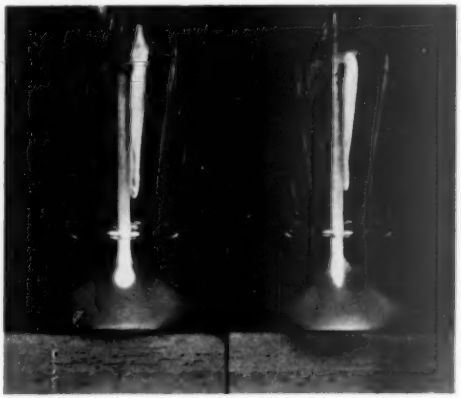


Fig. 1 Arcs in helium at 125 amp. d.c. s.p. $^1/_{16}$ in. diam. pure tungsten with molten ball left and $^1/_{16}$-in. thoriated tungsten with solid point end right

ARC THEORY

An arc has been defined[1] as a discharge of electricity between electrodes in a gas or vapor, which has a voltage drop at the cathode of the order of the minimum ionization potential or minimum exciting potential of the gas or vapor in which it burns. Of all the arc-welding processes, this inert-gas shielded-tungsten-arc process is the easiest to examine from the theory of gaseous conduction. These arcs are very stable and steady and have no metal transfer and no dissociation of polyatomic gases. The arc can best be described as a thermionic arc in which the current is carried principally by electron emission from a hot refractory cathode. The arc is always in electrical and thermal equilibrium. Voltage drop across the arc consists of the cathode drop, the drop in the positive column or arc flame and the anode drop. The cathode drop has received the most attention in the literature because it is more characteristic of the electrode material and the

surrounding atmosphere than the region around the anode.

The cathode is heated by positive-ion bombardment, but most of the current is carried by thermal emission of electrons from the cathode. These two factors attain a thermal and electrical equilibrium at a certain electrode temperature which depends upon the emission characteristics of the cathode material. Thoriated tungsten, having a lower work function than pure tungsten, does not require to be heated to as high a temperature in order to obtain the necessary emission. On the other hand, the cathode is cooled by electron emission, radiation and conduction. Cathode materials can be segregated into two general classifications—those having low boiling points, and the refractory types. Low-boiling-point cathode materials are characterized by high current densities which are concentrated in characteristic cathode spots, which move very rapidly at random over the surface of the cathode. Refractory materials such as carbon and tungsten have relatively lower current densities but have stable cathode spots. The presence of low-work-function impurities in refractory-type cathodes increases the current density in the cathode spot but does not adversely affect its stability. This characteristic makes possible the maintenance of a point on thoriated-tungsten electrodes. The low-work-function impurities usually used to promote the emissivity of tungsten electrodes are oxides of barium, strontium and thorium. The thorium oxide used in tungsten electrodes is mixed with the powdered tungsten before sintering and is distributed throughout the electrode so that there is always some at the end surface to promote these desirable effects.

"The arc column is the region between the cathode and anode in which the current is carried by the ionized gases. The thermal ionization that maintains the arc column is established by virtue of the high gas temperature which in turn is established and maintained by the energy given up by the ions and electrons in collision with the gas particles."[1] The anode region is characterized by a high electron space charge. The heat energy released at the anode is equal to the heat of condensation of the electrons. In this type of welding, the anode is usually a large cold surface of the piece to be welded. The principal effect of the anode material on the arc characteristics is that produced by the metal vaporized from the anode material which mixes with the ionized gas in the arc column. These metal vapors are also ionized and have much lower ionization potentials than the inert gases. Welding arcs can be classified as short arcs in which the arc column is $^1/_4$ in. and less in length and the effect of metal vapors in this space is significant.

ARC STARTING

The two methods of arc starting generally employed for inert-gas shielded-tungsten-arc welding are touching and withdrawing the electrode from the work, and the use of a high-frequency spark discharge between the

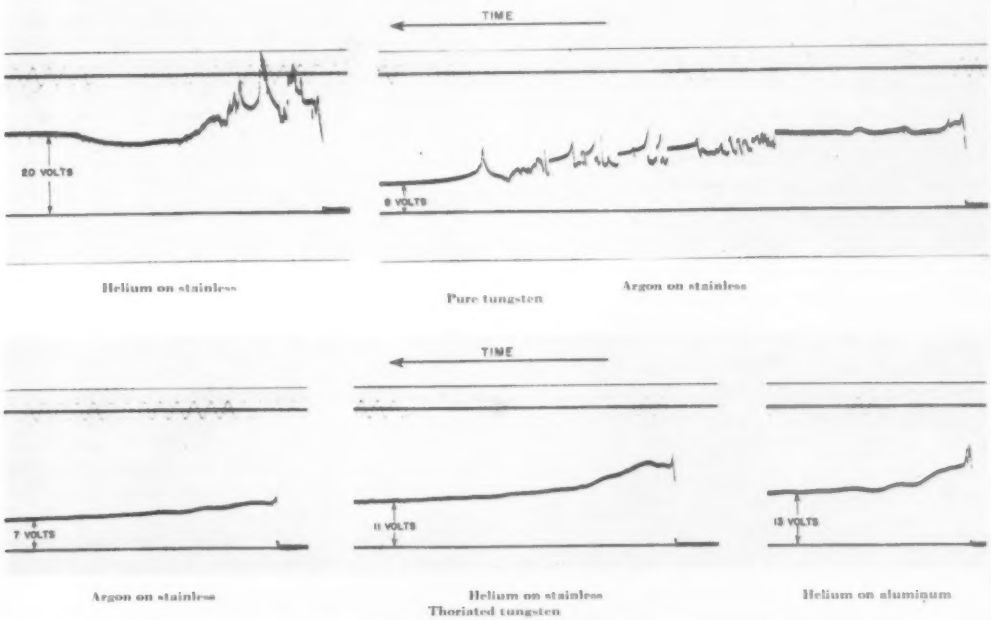


Fig. 14 Voltage oscillograms of arc starting by touching $^1/_{16}$-in. diameter electrodes at 75-amp. current

electrode and work. Touch starting is most common for manual welding using d.-c. straight polarity current. Starting the arc is a short-time phenomenon, and it occurs while the arc is sputtering, until the electrode becomes heated and settles down to a steady state. This starting produces a severe thermal shock at the end of the tungsten electrode and sometimes results in spitting off particles of tungsten, particularly when the welding current is high. However, starting with thoriated tungsten is not accompanied by sputtering and loss of tungsten.

The fact that the starting condition is a short-time phenomenon may best be shown by oscillograms of the arc voltage. Representative oscillograms of five conditions are reproduced in Fig. 1. Time duration of arc starting in argon with pure tungsten is about 25 cycles. The initial voltage is more than two times the final welding voltage and the sputtering period is shown by the jagged region of the oscillogram. The final voltage of the arc indicated on the extreme left is 8 v. In contrast, the arc-voltage curve during the starting period with thoriated electrodes is very simple, as shown in the oscillogram. The use of helium for shielding gas results in a considerably shorter starting period and the sputtering period occurs immediately after the electrode is withdrawn from the work. Thoriated electrodes with helium show a higher peak starting voltage than with argon but the period is relatively short and no sputtering is indicated. Other oscillograms of similar types of arc starting show somewhat different relationships, but these shown in Fig. 1 are representative.

The use of thoriated electrodes permits a greater range of operation for manual welding with touch starting. The factors affecting touch starting of the arc are shielding gas, current setting and open-circuit voltage of the welding generator. Difficulties are usually encountered in starting the arc at low currents. Table 1 shows comparative values of these limiting factors for pure and thoriated tungsten. The most favorable conditions for touch starting at minimum currents are argon shielding, high open-circuit voltage and thoriated tungsten. The material of the workpiece also has some effect but stainless steel is one of the easiest metals on which to strike. For instance, arcs can be readily started at currents as low as 5 amp. with thoriated electrodes, while successful starting with pure-tungsten electrodes cannot be accomplished below about 18 amp. Similar comparisons can be made with helium shielding except that the minimum current values are higher than with argon. The condition of having the arc climb up the electrode at low current values with pure tungsten is avoided with the use of thoriated electrodes.

Table 1—Minimum Conditions of Current and Voltage for Manual Welding with Touch Starting

Electrode Diameter, in.	Electrode Type	Shielding gas	Workpiece	Open circuit, v.	Normal Arc Volts	Normal Arc Current, amp.
0.040	Pure	Helium	Stainless	75	15	30
			Copper	..	23	33
		Argon	Stainless	..	10	18
			Copper	..	10	20
0.040	Thoriated	Helium	Stainless	..	17	17
			Copper	..	25	12
			Aluminum	..	27	10
		Argon	Stainless	..	11	5
			Copper	..	12	7
			Aluminum	..	8	13
		Helium	Stainless	50	14	33
		Argon	Stainless	20	9	14

Table 2—Starting D.-C. Straight Polarity Arc with High Frequency

Electrode Diameter, in.	Electrode Type	Shielding gas	Arc length, in.	Current, amp.	Starts in ten tries: Instant	Delayed	None
0.040	Pure	A	1/32	40	4	6	0
			1/16	..	3	7	0
			3/32	..	3	7	0
			1/8	..	1	8	1
0.040	Thoriated	A	3/32	40	10	0	0
			1/8	..	10	0	0
0.040	Thoriated	A	1/16	24	10	0	0
			...	16	8	2	0
			...	12	3	5	2
			...	10	..	..	10
3/32	Pure	He	1/16	125	2	1	7
		A	...	...	9	1	0
3/32	Thoriated	He	1/16	125	10	0	0
			2/32	...	8	2	0
			1/8	...	7	3	0
3/32	Thoriated	A	1/8	125	10	0	0
			2/16	...	10	0	0
			1/8	...	7	3	0
3/32	Thoriated	A 5%	1/8	125	10	0	0
		He 95%	2/16	...	5	3	2

Starting direct-current arcs with a high-frequency spark discharge is quite often desirable for semiautomatic welding particularly when the welds are made at frequent intervals. One of the aggravating difficulties quite often encountered in this high-frequency starting is not having the power arc initiated by the spark discharge. Sometimes the power arc comes on with a slight delay after the spark discharge is started but it is desirable to have a simultaneous start of the power arc with the high frequency. There are several factors which affect high-frequency starting and these can be classified in two catagories. One is the power equipment and circuit condition and the other is arc-gap conditions. The most significant factor in the arc starting is the current value which is usually dependent on the welding job and the lower the current required, the more difficult the starting condition will be. An increase of the open-circuit voltage of the welding generator and the power of the high-frequency oscillator will help but quite often this is not possible. The most practical way to improve arc-starting conditions is to shorten the arc length, use argon shielding and thoriated-tungsten electrodes, instead of pure-tungsten electrodes. Of these factors, the easiest one to change is the electrode, and the comparative effect of pure and thoriated electrodes on arc starting is shown in Table 2. Since the high-frequency spark is intermittent in na-

ture, the results obtained with critical conditions are not uniform. For this reason it was necessary to make a number of tries and record the average results. The number and type of starts in ten tries were used as the criterion and are recorded in Table 2. All these results were obtained with an open-circuit voltage of 80 and the same high-frequency oscillator. The best starting conditions were obtained with thoriated electrodes and argon shielding and consistent starts could be made with $^1/_8$-in. arc length and 40-amp. minimum current. The comparative results of the other factors can be evaluated by a study of the tabulated results. It should be noted, however, that a small amount of argon mixed with the helium results in a marked improvement in the starting compared to pure helium and this mixture still retains the welding characteristics of pure helium.

ARC VOLTAGE

The use of thoriated electrodes results in a reduced arc voltage over that obtained with pure-tungsten electrodes. Since the physical factor changed was the emission characteristic of the electrode, it is reasonable to assume that this reduction in voltage occurs in the cathode drop at the electrode. These differences in arc voltage can best be shown by the voltage gradient curves in Fig. 2. These curves are direct recordings of arc voltage on the chart of a recording voltmeter. This was accomplished by traversing the arc over a sloping workpiece until the electrode touched the work. This point is represented on the charts as the low point on the voltage curve and represents the voltage at 0 arc length. The travel speed of the arc and the speed of the recording paper in the meter can be correlated as indicated, to show the arc length at any instant. This makes a very simple and positive method of obtaining voltage gradients for any arc condition. The voltage gradients are almost a straight line between arc lengths of 0 and $^3/_{16}$ in. Voltage gradients with helium shielding are higher than those with argon by a factor of 2 to 5 depending on the current value and the composition of the workpiece. This well-known characteristic of helium facilitates the use of arc-voltage control with an automatic head. However, the most interesting thing shown by these charts is the voltage at 0 arc length. At the instant of arc extinction the voltage shown will be the sum of the anode and cathode drop. These values which are of the order of from 6 to 11 v. are very low compared to what might be expected from the ionization potentials of helium and argon which are 24.5 and 15.9, respectively. However, these extinction voltages are of the order of the ionization potential of iron which is 7.83. This would indicate that the metal vapors in the arc are ionized in preference to the inert gases. On the other hand, the voltage gradients are linear and show wide differences between argon and helium and the entire question of ionization in the arc is difficult to understand. This simple test procedure would seem to present a significant method for fundamental arc investigation, but this is beyond the scope of this paper.

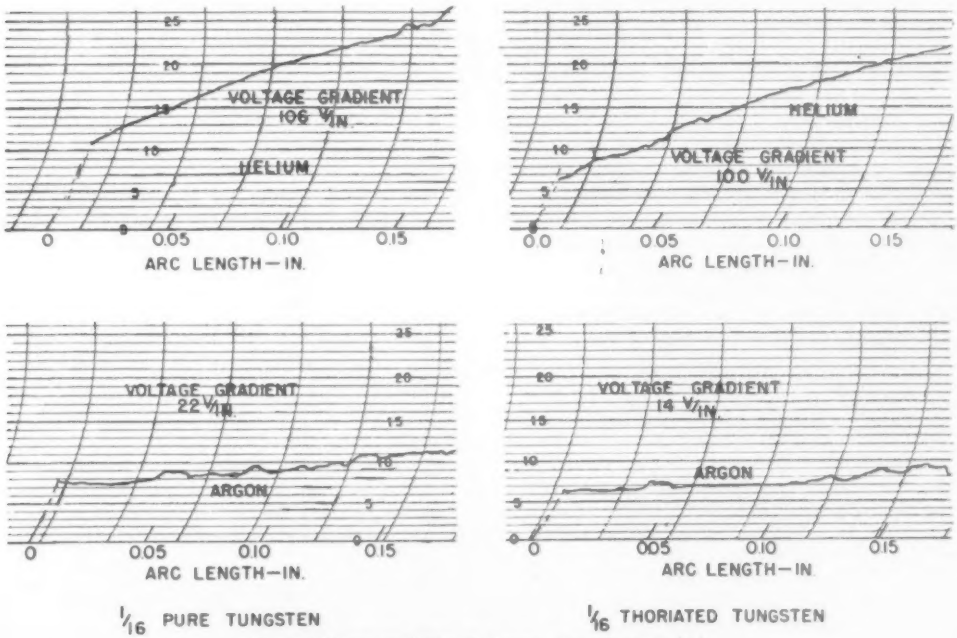


Fig. 2 Voltage gradients at 50-amp. d.-c. electrode (−)

The differences in voltage between the two electrodes at a current value of 50 amp. is about 4 v. with helium shielding, and 1½ with argon shielding. This significant drop in voltage of from 20 to 30% represents a loss in the heat of the arc which would apparently have to be made up by increased current to obtain comparable welding speeds. Currents have to be increased when thoriated tungsten is used but not to the extent as indicated by the decrease in voltage. Apparently only a portion of the arc heat generated in the cathode drop is effective as welding heat absorbed by the workpiece. The beneficial effects of the reduced cathode voltage drop are less radiated heat and less heat absorbed by the electrode holder.

CURRENT CAPACITIES

There has never been complete agreement on the current capacity of tungsten electrodes primarily because so many variables affect this rating. Besides type of current, polarity, shield gas and rate of flow, the type and physical dimensions of the electrode holder have considerable effect on the amount of current each size of electrode will carry. The holder used in these tests has a water-cooled metallic nozzle and the distance from the end of the electrode to the collet was 1⅛ in.

The present tests were all made with d.-c. straight polarity and the thoriated-tungsten electrodes showed considerably higher usable current capacity than pure tungsten in sizes larger than 3/32 in. and when helium was used for shielding gas. The results of these tests are shown in Table 3. Failure from excessive current of the 0.040 and 1/16-in. diameter sizes was in all cases indicated by the sudden breakdown or melting off of the lower 1/8-in. section. The electrodes were usable up to this breakdown current and the tabulated results shown provide a margin of 5 to 10 amp. from the breakdown current. It is interesting to note that there is no significant difference between type of electrode and shielding gas in these two sizes. The larger-size electrodes with argon shielding fail in the same manner again with no marked difference between pure- and thoriated-tungsten electrodes. The larger sizes with helium shielding, however, show considerable differences in maximum usable current values and type of failure. Pure tungsten is not practical to use when the current is increased to the value where the molten end moves about with the consequent uncontrollable wandering of the arc. The maximum current that the electrode will carry without metal transfer is considerably higher than this value. The thoriated electrodes, 3/32 in. and larger, with helium shielding fail by the sudden melting off of a large length and they are usable for welding up to this point. The data also indicate that the current capacities of the larger thoriated electrodes are higher for helium than for argon shielding. This could be the effect of the higher specific heat and thermal conductivity of the helium in cooling the electrode.

Table 3—Maximum Welding Currents for Tungsten Electrodes D.-C. Straight Polarity

Electrode		Helium, shielded	Argon, shielded
Diameter, in.	*Type*		
0.040	Pure	100	100
	Thoriated	100,	90
1/16	Pure	190	190
	Thoriated	190	190
3/32	Pure	250	350
	Thoriated	375	340
1/8	Pure	300	>500
	Thoriated	>500	>500

One function of the electrode other than forming the cathode of the arc is to conduct a current from the collet of the electrode holder to the tip of the electrode. This function of the electrode was investigated by extending the electrode 5/8 in. beyond the gas nozzle and short circuiting it on a strip of a high-melting-point material such as molybdenum. With the shielding gas flowing, current was passed through the electrode until it was heated to its melting point by its electrical resistance. The voltage drop across the tungsten was measured by a voltmeter. With the voltage drop and the corresponding welding-current values, the resistance of this 1⅜-in. long section of the electrode could be readily calculated. Both the voltage drop and the calculated resistance of 1/16 -in. diameter pure and thoriated-tungsten electrodes were measured and plotted in Fig. 3. This chart shows that the increase in resistance of the electrode materials from room temperature to the point of failure is in the order of 20 times. The voltage drop across the electrode is also appreciable and ranges up to about 5 v. Even in the normal current range these voltage drops would make a significant difference in any study of arc characteristics.

The temperature of the section of the electrode extending beyond the gas nozzle was measured by an opti-

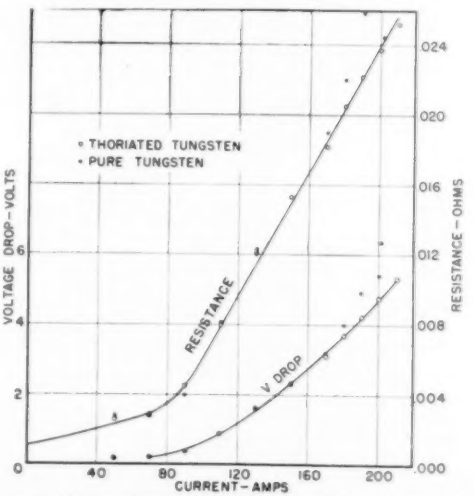


Fig. 3 Voltage drop and resistance of 1⅜-in. length of 1/16-in. diameter tungsten electrode

cal pyrometer. These readings were corrected for black-body radiation and are plotted in Fig. 4. This chart shows the relation of the temperature of the tungsten to the short-circuit current and also to the internal resistance of the electrode. It is interesting to note that the temperatures were measured up to the melting point of tungsten (6100° F.). The failure occurred by the melting of a section of electrode similar to a fuse action at practically the same current value as the maximum current-carrying capacity when used in an arc. It can therefore be concluded that the limiting factor in current-carrying capacity of thoriated- or pure-tungsten electrodes is simply their electrical resistance. Both Fig. 3 and Fig. 4 show no significant difference between pure and thoriated tungsten. The relation of resistance of tungsten to temperature corresponds closely to that shown in the literature.[2]

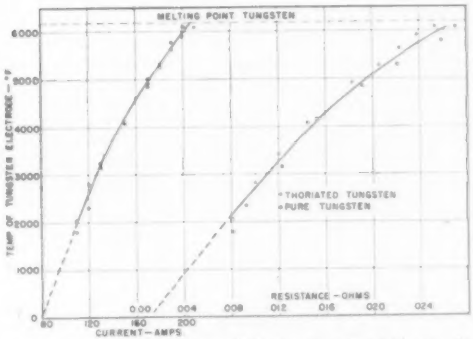


Fig. 4 Resistance heating of 1⅛-in. length of 1/16-in. diameter tungsten electrode

USE OF THORIATED ELECTRODES WITH ALTERNATING CURRENT

Touch starting on the work is not practical with high-frequency stabilized a.-c. welding and no beneficial results of thoriated electrodes can be realized on this score. High-frequency starting apparently is not significantly improved with thoriated electrodes. However, the current-carrying capacity is in some cases higher than that obtained with pure electrodes of the same diameter. This difference varies from 0 to 50% greater current-carrying capacity and it is apparently dependent on the d.-c. component and the amount of rectification in the arc. These factors, of course, are influenced by the choice of welding machine and high-frequency oscillator used. Any appreciable increase in current-carrying capacity is indicated by the fact that the ends of thoriated electrodes do not melt. It can generally be concluded that the advantages of thoriated-tungsten electrodes for welding with alternating current are less than for direct current.

TUNGSTEN-CONSUMPTION TESTS

A discussion of the many factors affecting the consumption of the tungsten electrodes is beyond the scope of this paper.[3] Laboratory tests designed to show comparative electrode consumption between thoriated- and pure-tungsten electrodes were not conclusive. When the welding conditions investigated were similar to normal operation, the consumption over a reasonable length of time could not be measured in terms of length or diameter. However, the weight loss of the electrodes could be measured with a chemical balance. The following limited tests are considered worth while reporting.

Continuous arc, 20 min., 325 amp., d. c., s.p. Helium shielded

1/8 in. diameter, pure tungsten, wt. losses, gm	0.0592
1/8 in. diameter, thoriated tungsten, wt. losses, gm.	0.0102

55 touch starts, 225 amp., d. c., s.p. helium shielded

1/8 in. diameter, pure tungsten, wt. losses, gm.	0.0143
3/32 in. diameter, thoriated tungsten, wt. losses, gm.	0.0040

55 high-frequency starts, a. c. argon shielded

1/16 in. diameter, pure tungsten, wt. losses, gm.	0.0077
1/16 in. diameter thoriated tungsten, wt. losses, gm.	0.0094

These losses are very small and probably represent material vaporized from the surface of the electrode. However, they do indicate less loss for thoriated electrodes with d.-c. straight polarity current.

Only in actual use on jobs can the important factors of accidental losses of contamination and oxidation be evaluated. One user reported that on a repetitive job involving manual welding at 100-amp. d. c. with argon shielding the weekly consumption of 1/16-in. diameter electrode per operator was reduced from 36 in. with pure tungsten to 6 in. with thoriated. Another production test run on a similar manual-welding job reported a 5-to-1 reduction in tungsten consumption with the use of thoriated electrodes.

THORIA CONTENT OF ELECTRODES

All of the laboratory and production tests reported were made with electrodes containing a nominal 1% thoria. Other electrodes containing 2 and 15% thoria were also investigated but the 1% thoria was decided as the most practical for production use. Practically no difference between the 1 and 2% thoriated types could be detected by any of the arc-characteristic tests. However, the 15% thoria electrodes did show better starting characteristics below 15-amp. welding current but since little commercial welding is done in this range there would be slight advantages in their use. Increased thoria content beyond 1% does have disadvantages in cost and fragility. The 15% thoria types are extremely fragile and are difficult to manufacture to accurate tolerances.

There still remains the question, "Is the 1% thoria sufficient to maintain uniform arc characteristics with continuous use or will the thoria gradually disappear from the surface and thus become ineffective?" When pure-tungsten electrodes are coated with barium oxide the effect produced on arc characteristics is practically the same as that of the thoriated tungsten in which the

thoria is sintered with the powdered tungsten in the manufacturing process. However, after a few minutes, arcing the surface coating of barium oxide loses its effectiveness but throughout the various tests of thoriated tungsten the arc characteristics have remained the same even after 20 min. continuous burning. Apparently the small amount of evaporation of the electrode material exposes a sufficient supply of thoria on the surface of the electrode.

ARC STABILITY AND WORK CONTAMINATION

Arc instability or deviation of the arc from the line of travel with the electrode is somewhat reduced with thoriated electrodes. This is probably the result of the higher current densities at the tip of the electrode and the ability of the electrode to hold a point. Arc stability is difficult to demonstrate by laboratory tests but it has been noted in commercial practice. Another factor closely related to electrode consumption is inclusion of tungsten particles in welds. This generally occurs when the electrode is accidentally touched to the molten pool or the filler rod. Experience in the laboratory and commercial use have shown less tungsten inclusions when thoriated electrodes have been used in place of pure tungsten. These inclusions are undesirable for a number of reasons but they are particularly troublesome in ferrous welding where the weld must be finished because of the formation of hard spots which are difficult to grind.

SUMMARY

Thoriated-tungsten electrodes were introduced about a year ago along with the d.-c. method of welding aluminum with the tungsten-arc process. It must be pointed out that its characteristics are not essential for welding aluminum nor are they limited to aluminum. The advantages of the use of thoriated tungsten apply equally to all d.-c. straight polarity, inert-gas shielded-tungsten-arc welding including stainless steel, copper, carbon steel, titanium, etc.

The economic advantages offered by the use of thoriated-tungsten electrodes are principally in reducing time lost due to contamination, poor arc striking and electrode adjustment.

References

1. Cobine, J. D., *Gaseous Conductors*, McGraw-Hill Book Co., New York.
2. "Temperature, Its Measurement and Control," 1939 Symposium, American Institute of Physics.
3. Tuthill, R. W., "Tungsten: To Save or Not to Save," *Weld. Eng.* (March 1949).

CURRENT WELDING LITERATURE

Airplane Engine Manufacture. Welding on Gas Turbine Engines for Aircraft, H. E. Lardge. Inst. Welding—Trans., vol. 14, no. 3 (June 1951), pp. 85–91.

Airplanes, Fuel Tanks. Manufacture of Wing Tip Fuel Tanks at Rohr Requires Unusual Forming and Welding Methods, G. R. Brolaski. Western Metals, vol. 9, no. 9 (Sept. 1951), pp. 40–41.

Automobile Manufacture. Machine Seamwelds Front Fenders Faster, W. G. Patton. Iron Age, vol. 168, no. 23 (Dec. 13, 1951), pp. 134–135.

Brazed Assemblies for Cost Reduction, N. M. Salkover. Tooling & Production, vol. 17, no. 7 (Oct. 1951), pp. 110, 112, 114–115.

Brazing, Electric. Induction Brazing of Large Rotors, P. H. Brace and E. B. Fitzgerald. Westinghouse Engr., vol. 11, no. 6 (Nov. 1951), pp. 184–185.

Carbides. Important New Carbide Developed by Carboloy. Machy. (N. Y.), vol. 58, no. 3 (Nov. 1951), pp. 185–186.

Destroyers, Masts. Aircomatic Process Used in Welding Aluminum Masts for Navy. Mar. Eng. & Shipg. Rev., vol. 56, no. 9 (Sept. 1951), pp. 58–60.

Electric Arc, Inert Gas. British Experience in Argon Arc Welding of Aluminium, W. V. Binstead and E. G. West. Inst. Welding—Trans., vol. 14, no. 4 (Aug. 1951), pp. 121–139.

Electric Arc, Inert Gas. Review of Theory and Practice of Inert-Gas Shielded-Arc Welding, W. J. Jackson. Sheet Metal Industries, vol. 28, no. 296 (Dec. 1951), pp. 1131–1136.

Electric Arc, Stud. Stored Energy for Stud Welding. Industry & Welding, vol. 24, no. 10 (Oct. 1951), pp. 68, 70, 72–73, 106–107.

Electric Arc. Stud Welding of Light Alloys, D. C. G. Lees. Engineer, vol. 192, no. 4990 (Sept. 1951), pp. 347–348.

Electric Arc. Stud Welding on Aluminium Alloys, D. C. G. Lees. Welding & Metal Fabrication, vol. 19, no. 10 (Oct. 1951), pp. 390–391.

Electric Electrodes. Basic-Coated Arc Welding Electrodes, D. Seferian. Iron & Coal Trades Rev., vol. 162, no. 4340 (June 15, 1951), pp. 1369–1372.

Electric Electrodes. Color Markings for Electrode Identification. Nat. Elec. Mfrs. Assn.—Publ. no. EW2-1950 (Nov. 1950), 2 pp.

Electric Electrodes. Use Welding Electrodes Right—Don't Waste Them, E. J. Clement. Mill & Factory, vol. 49, no. 4 (Oct. 1951), pp. 127–130.

Electric Electrodes. Welding with Aluminium Bronzes, E. C. Mantle. Engineering, vol. 172, no. 4471 (Oct. 5, 1951), pp. 443–444.

Electric Heating, High Frequency. Induction Hardening and Brazing, R. J. Brown. Instn. Production Engrs.—J., vol. 30, no. 8 (Aug. 1951), pp. 436–451.

Electric Resistance, Light Metals. Recent Advances in Technique of Electric Spot Welding of Aluminium Alloys in French Aircraft Industry, J. Faguet. Inst. Welding—Trans., vol. 14, no. 4 (Aug. 1951), pp. 110–120.

Electric Resistance, Light Metals. Resistance Welding Light Alloys, R. Bushell. Welding & Metal Fabrication, vol. 19, no. 10 (Oct. 1951), pp. 387–389, 396.

Electric Resistance. Measurement of Resistance Welding Current, J. J. Riley. Industry & Welding, vol. 24, no. 10 (Oct. 1951), pp. 48–50, 118–119.

E. S. S. Welding Process, H. O. Willrich. Welding & Metal Fabrication, vol. 19, no. 10 (Oct. 1951), pp. 383–385; no. 11 (Nov.), pp. 435–437, 441.

Furniture Manufacture. Welds Instead of Bolts Cut Assembly Time 50%, W. Rudolph. Mill & Factory, vol. 49, no. 5 (Nov. 1951), pp. 128–129.

(*Continued on page 151*)

Flash Welding of Components for Aircraft and Similar Applications

Improvements and refinements in weld variable control, instrumentation, weld quality control, inspection and testing, designing for welding, etc., have contributed to the high level of confidence in the process and in its wider application

by J. H. Cooper

INTRODUCTION

THE past ten years have seen a greatly expanded usage of resistance flash-butt welding in the manufacture of aircraft components and similar parts requiring weld-quality levels in excess of those obtainable by other available welding methods. Such expanded usage of the process has brought with it an equal expansion of the size and welding capacity of the equipment. Increased work sizes call for end-to-end flash-butt welds in high-strength alloy tubing as large as 20 in. in diameter and welders with forging forces as high as 1,500,000 lb. The electrical ratings have advanced proportionately.

Improvements and refinements in weld-variable control, instrumentation, weld-quality control, inspection and testing, designing for welding, etc., have also contributed to the high level of confidence in the process and in its wider application. Figures 1–16 inclusive are illustrative of typical applications of the process.

GENERAL

The greater proportion of the materials welded are a group of related steel alloys such as S.A.E. Steels 4130, 4135, 4140, 4340, 8740; A.M.S. Steels 6371, 6324, 6413, 6359-A, etc.

These alloys are heat treated to high strengths after welding, often as high as 200,000 to 225,000 psi. ultimate, and the loading requirements in testing and field service are proportionately high.

Typical applications using these steels are aircraft components such as landing gear, struts, control arms and rods, hydraulic cylinder barrels and piston assemblies, steel propeller blades, jet-engine rings and a multiplicity of minor accessory items.

These flash-butt welded materials are also used to make items for military tanks and caterpillar tractors such as large precision bearing rings for tank turrets, track rollers made from two forged halves, track "bogie" arm assemblies from three relatively simple forgings, etc.

The petroleum industry uses flash-butt welding to join pipe-threaded "box and pin" joints to the ends of rotary drill pipe approximately 30 ft. long and as large as $7^1/_2$ in. O.D. by $^5/_{16}$-in wall. Typical materials are S.A.E. 4137 steel joints welded to S.A.E. 1040 steel pipe and the finished welds are heat-treated. The pipe lengths are threaded together for drilling to depths of several thousand feet. The welds simultaneously carry the weight of the drilling length, the stress reversal of pipe rotating with different sections in different planes, the torsional load of drilling and the internal high hydraulic pressure of the drilling "mud." It is a heavy-duty service which has been satisfactorily met over a period of years.

Stainless steels and nickel alloys are flash welded to make such items as jet-engine rings and shells, piston-engine-valve assemblies and many smaller stainless items.

In much lesser volume and on smaller parts, such materials as titanium, aluminum, magnesium, bronze, nickel-silver, copper and copper alloys, nichrome, bimetal etc., have been satisfactorily flash-butt welded, producing weld quality satisfactory for the service requirements.

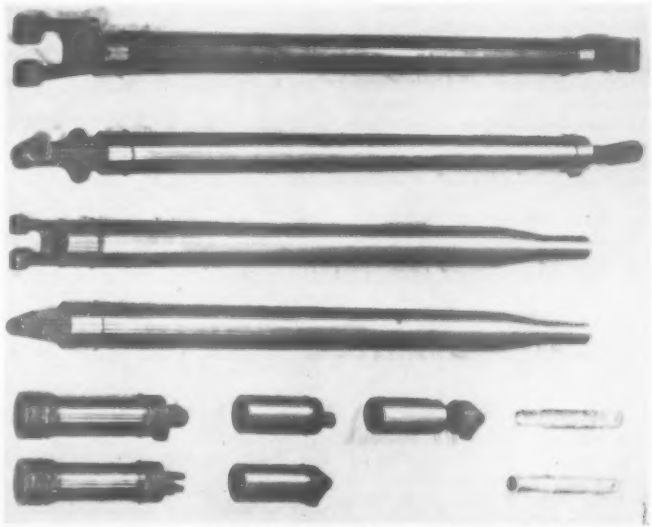


Fig. 1 Flash-welded alloy-steel aircraft components

J. H. Cooper is connected with the Taylor Winfield Corp., Warren, Ohio.

Paper presented at the Thirty-Second Annual Meeting, A.W.S., Detroit, Mich., week of Oct 15, 1951.

PROCESS CHARACTERISTICS

The flash-butt welding process offers a particular attraction to the aircraft industry through the medium of:

(*a*) Weight savings
(*b*) Greatly reduced welding time and
(*c*) Decreased cost of welding

Current governmental design specifications do not provide a direct means for a comparison of over-all joint efficiency between flash-butt welding and fusion welding such as gas, metallic-arc or inert-arc welding. Such a direct comparison cannot be made because specification references to the strength of flash-butt welds are in terms of tensile strength while references to the strength of fusion welds are in terms of shear strength. However, some appreciation of the higher-joint efficiency of flash-butt welding can be obtained from the fact that flash-butt welds in alloy-steel tubing which is heat treated after welding are permitted a design-allowable ultimate tensile stress of 80% of the minimum ultimate tensile stress to which the parts are treated. If, for example, the parts are treated to a range of 200,000 to 225,000 psi. then 80% of 200,000 is 160,000 psi. as the design-allowable ultimate tensile stress.

In comparison, fusion-welded joints using heat-treatable filler alloy and having the parts heat treated after welding, require submission of test results for special consideration. The design - allowable stresses permitted are 85% of the reliable minimum test values. Regardless of the test values, the design-allowable ultimate stresses cannot exceed 150,000 psi. for material thicknesses up to $^1/_8$-in. and 135,000 psi. for material thicknesses over $^1/_8$-in. It is not often that these values of maximum allowable stresses are used on production parts due to difficulties in obtaining sufficiently high reliable minimum test values.

Individual design practices for flash-butt welding vary with the product and service requirements as well as with different fabricators. For example, some designs contain cross sections increased 25% over the entire section to apply the 80% joint-efficiency factor. Other designs increase the cross section only in the vicinity of the weld by an amount dependent upon joint requirements and blend the thicker joint area to the thinner portion of the rest of the section.

The contrast between welding times required for flash welding and for fusion welding is so marked as to be comparable to 5 min. for a flash weld to 5 hr. for a fusion weld on a large multipass fusion-welded structure.

The combination of drastic time savings, weight savings, fewer reworks and rejects, the ability to semifinish machine to closer tolerances before welding, all obviously increases production and reduces the unit cost of the welded joint and structure.

The basic approach of the flash-butt welding process through the joining of parent metal to parent metal under high pressures and mechanized repetitive control provides most favorable conditions to minimize inclusions and porosity, to limit heat effect on the metal, to produce high uniformity of weld quality and to hold closer dimensional tolerances, as compared with any type of fusion welding now in common usage.

INSPECTION AND TESTING

No effort is spared in the inspection and testing of these aircraft and similar components to insure the highest and most uniform weld quality and joint efficiency possible.

No known *nondestructive* method of weld testing has been found to give a sensitive and infallible indication of the quality of a welded joint any more than such a test is available to judge the quality of the parent steel of the parts to be welded. Such nondestructive test methods as proof loading, radiographic tests, magnetic-particle tests, fluorescent and nonfluorescent penetrant tests, ultrasonic tests, magnetic-field tests, electrical-resistance tests, all yield certain helpful information within a limited range of test conditions. With the exception of proof loading and magnetic-particle testing, these various

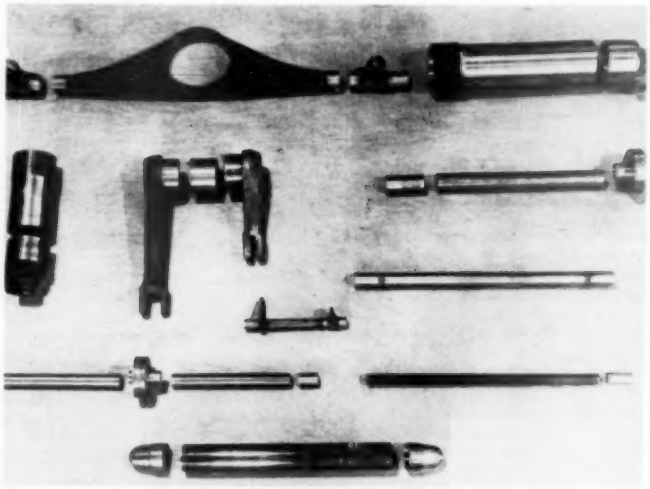


Fig. 2 Flash-welded alloy-steel aircraft components

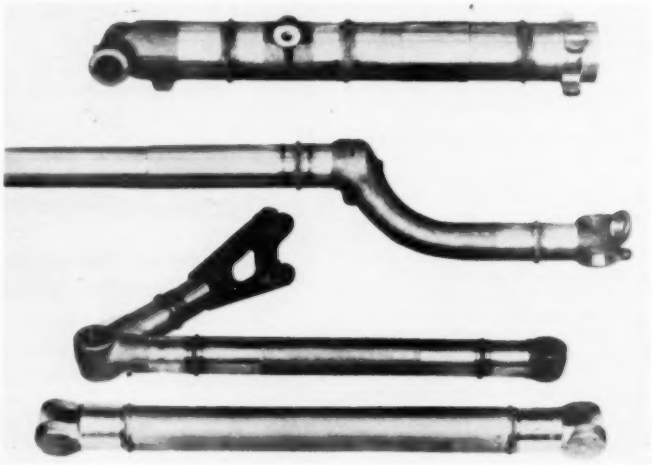


Fig. 3 Flash-welded alloy-steel landing-gear parts

Fig. 4 Flash welding of heavy-duty aircraft components at The Cleveland Pneumatic Tool Co.

methods have found only very minor acceptance to date.

In the absence of a sensitive and infallible nondestructive method of weld and joint testing, a combination of destructive and practical nondestructive tests must be used for process control.

One of the outstanding advantages of a mechanized process involving no added filler metal such as flash-butt welding, is the fact that the average quality level can be ascertained and maintained by the use of a very small percentage of destructive tests combined with a reasonable minimum of nondestructive tests.

Generally accepted process control consists of the following steps:

1. Actual production parts or a facsimile of the parts in the vicinity of the welded joint are used in a test group to establish the settings of the welding variables and to provide material for both nondestructive and destructive testing as well as determination of weld quality. Such test groups use the same materials, the same flash-removal practices and the same heat-treating procedures as are used in the manufacture of production parts.

Air Force-Navy Aeronautical Specifications, MIL-W-6873 (formerly AN-W-31) require a minimum of 15 specimens in an original test group.

Similarly check tests are made periodically to verify that welding quality and conditions have not varied subsequent to the establishment of the initial schedules, or to re-establish the quantity level when adjustments of welding variables or changes in material and treatment are made.

The welded parts in the test groups are then tested by cutting into coupons and possibly also full-size tests are made depending upon the size and geometry of the parts.

The tests applied to these groups are:

(*a*) *Visual*—with the amount and proportions of the upset material as well as surface conditions acting as a source of information to an experienced observer.

(*b*) *Magnetic Particle*—for freedom from defects, predominantly of the surface type, and checking in particular the area where upset metal has been removed before testing as well as the heat-effected zone.

(*c*) *Tensile*—this test is not a sensitive one and unless welds are exceptionally poor, welds tested in the as-welded condition will not fail in the weld while welds

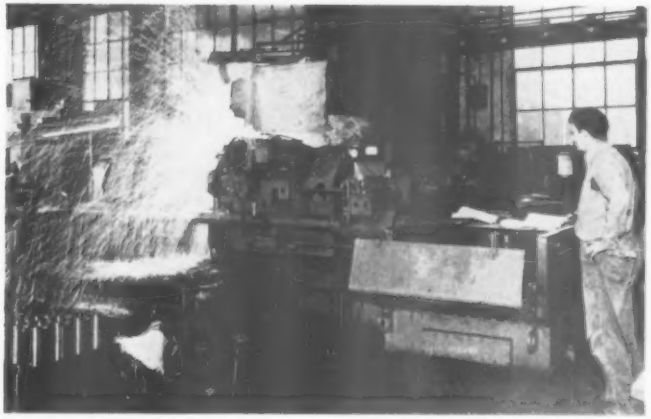


Fig. 5 Flash welder, 800-kva., in action at The Cleveland Pneumatic Tool Co.

Fig. 6 Flash-welded alloy-steel components produced by the Lockheed Aircraft Corp.

tested in the heat-treated condition can readily meet the generally accepted requirement that the tensile strength be no less than 95% of the average strength of the parent material and in each coupon the lowest weld strength shall not be less than 80% of the average strength of all coupons.

(*d*) *Free Bend*—this test is accepted as being the most indicative of weld quality and is used in a number of forms.

All upset metal is usually removed flush with the parent metal to provide a uniform section through the weld, and the material must be in the heat-treated condition for best results.

When the parts are tubular or solid rounds or squares, bends are made at all four quadrants in the sequence of 0-, 180-, 90- and 270-degree positions.

When the parts are solid flats, rectangles or curved coupons cut as segments from a large tube, bends are made around the longer axis of the section in both forward and reverse directions. Notches are sometimes cut at the ends of the weld if certain geometrical cross sections cannot otherwise be made to break through the weld.

These bend tests result in fractures which are predominantly through the weld and provide areas for examination as to the possible presence of voids, inclusions, discontinuities, type of fracture, etc., for comparison with similar fractures in unwelded parent metal.

No uniform standards have been established for the evaluation of such fractures and the analysis and acceptability of the test results depend greatly upon the material, the geometry and the service requirements of the fabricated product.

The angle of bend to that point at which fracture first starts is a relatively sensitive method for evaluating weld quality in these steels treated to high strengths.

(*e*) *Impact*—Occasionally during the test welding of a new material or setting up a new schedule, impact tests are made. The tensile type of impact testing is the best type due to difficulties in consistently locating the notch with accurate relationship to the center of the weld when making the notched-bar type of impact test.

2. Nondestructive tests run on production parts to supplement the tests described in (1) above are generally as follows and must be performed after heat treating if the production parts are to be heat treated after welding.

(*a*) *Proof Testing*—this test is either of the straight tensile or bending type depending upon the geometry of the production parts. If the bending type of test is made, such bends take place in all four quadrants of tubular parts and in both forward and reverse directions on flat sections. The bending test is often used on welds in straight tubular parts of large cross section due to limitations of large tensile-testing equipment.

While special-service requirements sometimes alter the frequency of taking proof-testing samples (even to a maximum of 100% of production) Air Force-Navy Aeronautical Specifications MIL-W-6873 specify the following sample frequency as the permissible minimum:

Lot Size	Sample Size
Less than 31	All
31 to 50	30
51 to 100	42
101 to 200	55
201 to 400	60
401 to 800	65
801 to 4000	70
4001 to 10,000	75

Again while special-service requirements alter individual applications, MIL-W-6873 requires that the proof load shall be at least 67% of the ultimate strength of the material, unless the part has a region weaker than the region of the weld, in which case the proof load shall be at least 67% of the weakest region.

Proof loading in excess of the minimum requirement of 67% is often practiced and a good test value is 80% of the tensile-yield strength of these heat-treated alloys.

3. *Flash Removal*—Flash is removed to approximately $^1/_{16}$ in. above the surfaces of the parent metal on quite large parts ranging down to $^1/_{64}$ in. on small parts, unless the design of the part necessitates its entire

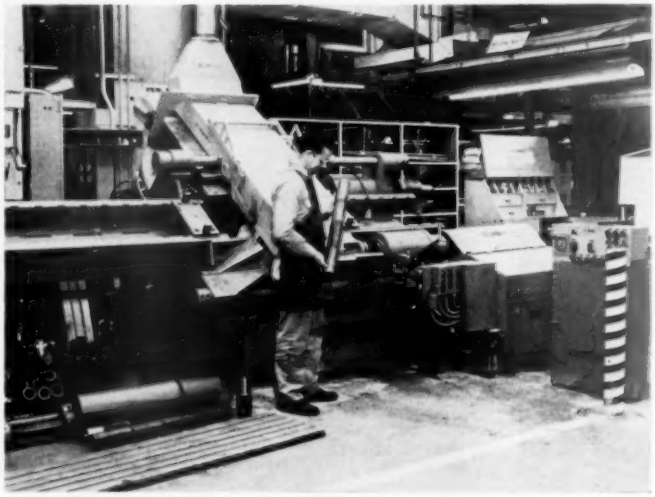


Fig. 7 Flash-welded joint in 5-in. diameter tubing, 6-sq. in. weld area, is produced in Cr-Ni-Mo steel by 400-kva. welder at Lockheed Aircraft Corp.

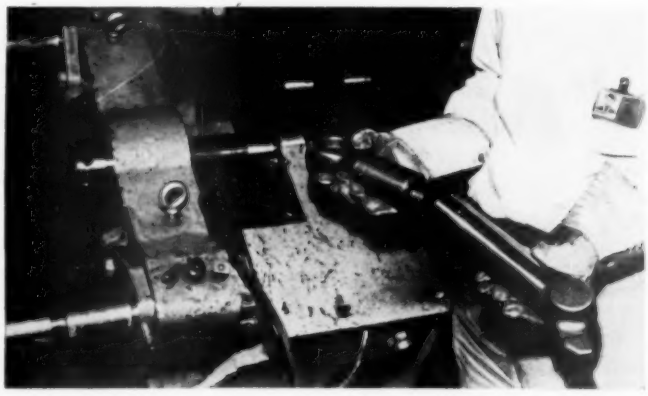


Fig. 8 Typical landing-gear component joined by flash welding at Northrop Aircraft, Inc., using 400-kva. welder

Fig. 9 Jet-engine turbine-wheel assembly produced by flash welding 3-in. diameter solid shaft to 3-in. embossment on wheel forging

Fig. 10 Jet engine rings of titanium 1 × 1¼ in. bar stock, are flash welded by the American Welding Company

removal. Generally the flash is not removed at all from the inside of hollow shapes unless necessitated by the design of the part.

4. *Alignment Tolerances*—Minimum requirements of MIL-W-6873 (generally exceeded in production) for alignment of welded sheet and tubular joints and measured near the weld before flash removal are 0.008 in. for work thickness up to and including 0.080 in. and 10% of work thickness for thicknesses exceeding 0.080 in.

The minimum requirement for alignment of joints in bars, measured near the weld before flash removal is 5% of the diameter or thickness.

Angular misalignment has the minimum requirement of not exceeding 0.005 in. per inch of length.

5. *Length Tolerances*—No uniform standards of tolerances on length dimensions of welded assemblies are available and practices of individual fabricators vary widely depending upon the product, its service requirements, the length tolerances of the individual parts prior to welding, the allowances for change during heat treatment, the amount of machining after welding, the care used in loading work parts in the machine, etc.

CONTROL OF PROCESS VARIABLES

1. Certain process variables are of the dimensional type and are functions of automatic machine performance after they have been initially established. Such variables are:

(*a*) The total amount of material flashed off
(*b*) The total amount of material upset
(*c*) The final die opening

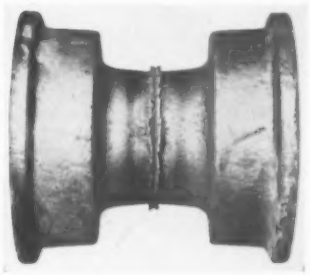


Fig. 11 Track rollers for army tanks and tractors are flash welded from two halves by the International Harvester Co.

Fig. 12 Three flash welds make up a complicated "bogie arm" for tank drive manufactured by the International Harvester Co.

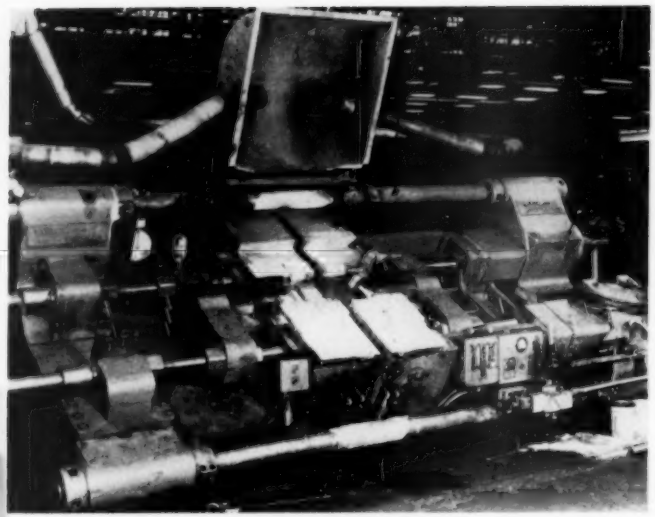


Fig. 13 Flash welder, 800-kva, at the International Harvester Co., showing "bogie arm" in machine

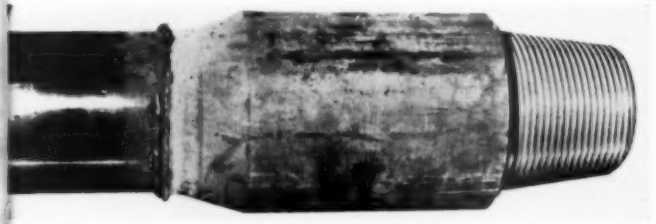


Fig. 14 Heat-treated flash welds are used by the major manufacturers of rotary drill pipe to join alloy-steel "pin and box" joints to the ends of the drill pipe

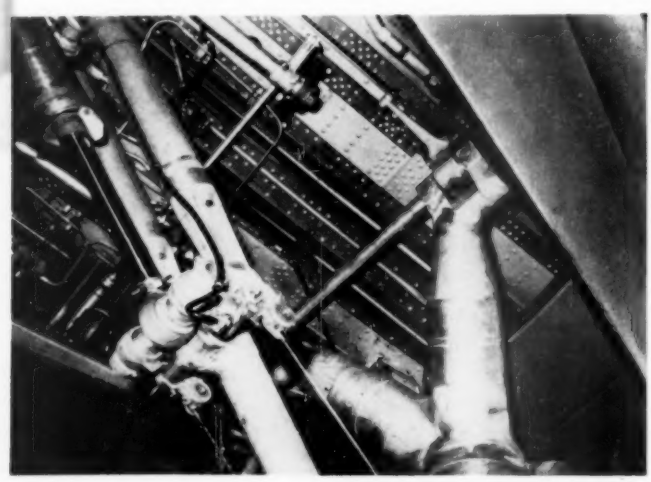


Fig. 15 Flash-welded main landing-gear assembly of Douglas Aircraft Co., Inc., Type C-124-A Globemaster II airplane

(*d*) The total amount of material lost (sum of *a* and *b*)

(*e*) The initial die opening (sum of *c* and *d*)

The control of the three basic dimensional variables (*a*, *b*, and *c*) is trending toward precision mechanisms using coarse and vernier scales to make the adjustments with a high degree of repetitive accuracy.

Those process variables represented by dimensions and tolerances of the parts to be welded are controlled by conventional quality control and inspection methods.

2. Some of the process variables are of the type involving time. The most important of these are:

(*a*) The flashing time and

(*b*) The upsetting current time

The control of the flashing time is obtained by various conventional mechanical electrical or hydraulic speed changers and the measurement is trending toward the use of visual indicating apparatus which is started and stopped automatically to indicate the actual flashing time. Apparatus of this type is typified by a precision counter with reset lever, providing a vernier dial graduated in tenths of seconds and having a totalizing dial for a maximum of 100 sec.

The control of the upsetting current time is similarly trending toward the use of an accurate electronic timer and measurement by the use of visual indicating apparatus which is automatically started and stopped to indicate the upsetting current time. Apparatus of this type is typified by a precision counter with reset lever, providing a vernier dial graduated in hundredths of seconds and having a totalizing dial for a maximum of 1000 sec.

Occasionally the additional variables of chill time and temper time follow the upsetting current time and are controlled and indicated in a manner similar to the control of the upsetting current time.

3. The control of the process variables of the flashing voltage and the upsetting voltage have a definite trend toward the use of the multiple-voltage-level type of control. This commonly takes the form of starting the flashing action at a high initial level until it is well stabilized, after which the voltage is automatically reduced to a substantially lower level, and this level is in turn followed by automatic reduction to a third and still lower level during upset. These various levels are generally controlled by the use of a combination of welder-transformer primary taps and phase-shifting heat control.

An a.c. voltmeter connected to the secondary of the welder transformer indicates with accuracy the open-circuit voltage as determined by the value of the line voltage on the transformer primary, by the setting of the phase-shifting heat control and by the setting of the transformer

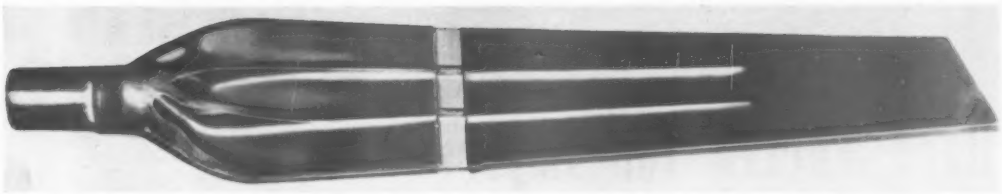


Fig. 16 Flash butt-welding is used by the Aeroproducts Div., General Motors Corp., to produce the main thrust member from two smaller forgings in the manufacture of large hollow alloy-steel propeller blades

tap switch. This meter also indicates with a lesser degree of accuracy comparative conditions during flashing and upsetting.

When a temper time is used, the tempering voltage is adjustable in a manner similar to the adjustment of the upsetting voltage.

4. The most important process variables involving forces are:

(*a*) Upsetting force and
(*b*) Clamping force

The commonly accepted practice of control of the upsetting force is to have available at all times the maximum upsetting force of which the apparatus is capable. This ensures maximum speed of closure of the flashing gap between the workpieces and maximum speed of upset. The work will automatically absorb that portion of the total available force as determined by the resistance of the plastic-weld area and the remainder of the total force will be automatically absorbed by the machine. This practice also contributes greatly to holding close tolerances of over-all length of welded assemblies.

The preponderance of welding equipment performing the type of welding covered by this paper is hydraulically operated and the total available upsetting force is indicated by adequate gages in the hydraulic pump and accumulator oil-pressure circuits.

The clamping forces are indicated by similar pressure gages after adjustment is made by adequate pressure-regulating apparatus.

The initiation of flashing of large sections, particularly ones of compact cross section is more readily accomplished by the use of low flashing force, and hydraulic apparatus for adjusting this force is provided on most machines. This is in addition to beveling of the parts when preparing for welding.

Gages in the hydraulic circuit indicate the value of the flashing force.

5. The practice of using a controlled shielding atmosphere around the flashing area is becoming more and more prevalent to assist in obtaining a homogeneous metallurgical structure. Such shielding atmosphere is usually dried city gas, helium or argon and its application and control requires the necessary flow meters, pressure reducers, gages, solenoid valves, sequencing controls, piping, etc.

The atmosphere is usually applied internally in hollow sections and externally for solid sections. Very low gas pressures are used, with just enough pressure and flow rate to insure an enveloping blanket with a minimum of gas consumption.

RECORDING INSTRUMENTATION

The use of instrumentation of the recording type varies widely with the product to be welded and previous experience of the fabricator with the flash-butt welding process.

In those applications where the parts to be welded have reached an expensive stage of manufacture at the time of welding, records are often taken of certain key welding variables each time a weld is made. Such records are permanently identified with the welded assemblies for possible future reference.

In applications involving more production of items with lesser unit value, records are taken only periodically as spot checks.

Fabricators gaining initial experience with the process often take records of all welds until a sense of values and basic data have been acquired, after which only spot checks may be recorded.

The variables generally recorded consist of:

(*a*) The welding current
(*b*) The upsetting force
(*c*) The movement of the work parts relative to each other

Since the speed of the record charts is known and is constant, those variables involving times, velocities and speeds are thus also recorded. The charts of *a*, *b* and *c* are synchronized to permit simultaneous checks of the status of the variables on all charts.

The welding current is obtained through the medium of a current transformer in the primary circuit of the welder transformer plus the knowledge of the welder-transformer turns ratio.

The upsetting force is obtained indirectly through measurement of hydraulic pressures in the actuating system or through the use of built-in strain-measuring mechanisms in the welder.

The movement of the parts relative to each other is translated from a mechanical movement to variation of an electrical resistance. The variation of this resistance in a circuit including an accurate reference level of voltage, provides an output voltage proportional to the work movement, which voltage is in turn properly recorded.

The actual recording means usually consists of synchronized inked charts operated by a single drive with variable speed through pick-off gears. Such recording apparatus is typified by Esterline-Angus recording meters and operates at relatively high speeds (up to 180 in./min.) with starting and stopping of the charts automatically accomplished by the electrical sequencing of the welder. The speed of response of such instruments is not as high as that of the self-inking oscillographs mentioned below but in some cases this is an advantage in that the "envelope" of the variable recorded is more subject to interpretation than the nonrepetitive instantaneous values recorded by the more sensitive oscillographs. The upsetting force is indirectly indicated only through hydraulic pressures with such an Esterline-type instrument and because of the previously mentioned relatively low speed of response, the inked trace is closer to static rather than dynamic values.

Some fabricators prefer the Brush self-inking magnetic oscillograph as a recording means in spite of the greater complexity, more extensive calibration requirements, higher initial expense, etc., in order to have a more flexible and sensitive instrument for additional uses. Also, this type of apparatus is essential when using the strain-gage method of indicating upsetting forces.

No fabricator claims the use of such recording instrumentation guarantees weld quality but it is generally agreed that the more data available through the use of such instruments, the better the possibilities of maintaining high-quality levels. In the event of a service failure, a permanent record showing no deviation of the recorded variables from normal often changes the trend of thought as to some other possible cause of a failure.

ACKNOWLEDGMENTS

The cooperation of the various fabricators in furnishing photographs and information regarding production procedures is herewith gratefully acknowledged and is sincerely appreciated.

Hard Facing for Impact

Some of the aspects of impact as a wear factor are analyzed and data supplied to enable engineers to select appropriate hard-facing alloys

by Howard S. Avery

Abstract

Wear, defined as deterioration due to use, is caused primarily by mechanical, chemical and thermal factors. The mechanical factors operate through stress and impact is one of the important stress producers.

Hard facing, which is one of the important techniques for combatting wear, can provide effective protection against deterioration from impact provided the alloys are properly selected and used. The most appropriate hard-facing materials for such service are martensitic irons, martensitic steels, and austenitic steels; which are considered suitable for light, medium and heavy impact, respectively.

Static and dynamic compression testing are described, together with data for important hard-facing alloys, while the importance of engineering to avoid tension is stressed. Critical limitations of impact energy and velocity are suggested to minimize structural damage and to help in alloy selection.

The three hard-facing alloy types are described in some detail to guide the engineer in their selection. A few representative applications are given and the advantage of composite structures in wear control is shown. This paper is intended to help engineers cope with the problems of industrial wear and includes some pertinent data on abrasion.

INTRODUCTION

IMPACT is a serious cause of wear. It not only can produce sudden failure, but can also induce progressive deterioration that will lead to a loss of serviceability.

Impact may operate alone under conditions where it appears as an obvious danger. In many more cases it combines with other important wear factors to create a situation that requires considerable judgment in the selection of suitable wear-resistant materials.

Hard facing is a versatile weapon for combating wear. In comparison with homogeneous metallic parts it has several advantages, which include applicability to the exact area where wear is focused, ready use of very hard and wear-resistant compounds, ease of application outside of manufacturing plants, economy in the effective use of expensive alloying elements and protection in depth. Since hard facing to many implies hard, brittle materials that may shatter readily under impact, its full utility for protection under impact conditions has not been attained.

As a matter of fact a considerable proportion of facing alloy is used where heavy repeated impacts are the salient wear factor in service. The metals for such use are tough, work-hardenable steels. However, there are many other areas where lighter impact is important and where hard overlays with quite different properties are appropriate but have not been used because engineering data that would guide their selection have been lacking.

It is the purpose of this paper to analyze some of the aspects of impact as a wear factor, to identify several engineering properties that are pertinent and and to describe hard-facing alloys that may appropriately be applied.

IMPORTANT TYPES OF WEAR

A dictionary definition of wear is deterioration due to use. In a simple classification wear can be attributed to three basic factors: thermal, chemical and mechanical. However, these not only combine and interact but also each of them can operate with many variations.

Mechanical wear is the result of stress. Complexity arises from the many ways in which stress may be applied and distributed. If the stresses are applied through hard particles, usually of a foreign and nonmetallic material, the wear is typed as erosion or abrasion. If the stress arises from collapsing bubbles in a turbulent fluid, it may produce cavitation erosion. If, from the interference of metal surfaces in motion, it can be called frictional. Stress, by forcing clean surfaces into intimate contact, may also cause seizure, galling, scuffing, scoring and the like. These phenomena may be typed as welding wear, and are usually associated with friction under conditions of poor or incomplete lubrication.

Impact is another mechanical stress producer. Though these stresses may be brief, since impact is characteristically a momentary phenomenon, they may be astonishingly high. A homely example will demon-

Howard S. Avery is connected with the American Brake Shoe Co., Mahwah, N. J.

Paper was presented before the Thirty-Second Annual Meeting, A.W.S., Detroit, Mich., week of Oct. 15, 1951.

strate this. If a center punch or a small drift punch is slowly pushed against the face of a mild steel plate with all the strength of a strong man, only a tiny scar or minute indentation is likely to result, but a child with a light hammer blow on the punch can produce a much deeper indentation. Indeed, impact ranks along with the lever as a means for obtaining a marked mechanical advantage.

The chemical aspect of wear is usually evidenced as corrosion or oxidation and scaling. Temperature accelerates both of these and in addition modifies resistance to mechanical stress by softening metals and permitting easier plastic flow. These interactions make wear analysis a complex matter, though fortunately the thermal and chemical factors are not associated with most impact situations, and will be neglected here. Further discussion of some aspects of hot wear may be found in a previous paper.[1]

A practical approach to the selection of materials for impact resistance is to classify impact as light, medium or heavy and designate the appropriate alloys for each. Experience indicates that these are martensitic irons, martensitic steels and austenitic steels, respectively. Other materials can be interchanged with these if different properties, such as heat resistance, corrosion resistance, lack of magnetism, etc., are needed and the appropriate mechanical properties are known.

The qualitative terms above are convenient but an engineer will want to know where light impact ends and heavy impact begins, and will also need quantitative values that will permit intelligent substitution. The previous lack of such information has led to considerable experimental work. The resulting data and calculations, while less complete and rigorous than we would like, are offered as a preliminary foundation for dealing with the problem.

A satisfactory material subjected to impact should not fail in service. This usually implies that it should not crack, though there are important exceptions. Some cracked specimens have absorbed amazing amounts of impact energy without becoming unserviceable, and even when deterioration is progressive, economical performance before failure may be obtained.

Resistance to failure implies strength and in the case of hard facing this is usually compressive strength. It may also imply toughness and the common reaction to impact conditions is to specify a tough material. However, a second requirement dimensional stability, may be important. This also implies strength, but unfortunately the higher levels of elastic strength are attained at a sacrifice of ductility, which is a component of toughness.

Dimensional stability requires a minimum of plastic flow or permanent deformation under impact. In seeking high elastic strength some point is reached where danger of cracking becomes too great, and flow is accepted as a lesser evil. Thus an engineer may be forced to some compromise in selecting materials. A similar compromise is common in dealing with impact and abrasion together since abrasion resistance frequently parallels elastic strength.

It is important to distinguish between flow resistance and crack resistance, both of which are loosely termed impact resistance though they are generally opposing properties. Since both can cause deterioration during the use of engineering materials, they are included here as facets of the wear problem.

TOUGHNESS

Resistance to fracture is implied by the term toughness. There are several methods of describing this quality, but one of the simplest is to report the energy absorbed in a test up to the point of fracture. Conventional Charpy, Izod, and tensile impact tests do this. It also can be done for standardized tensile tests.

For simple uniaxial tension the energy used in breaking a specimen can be taken as the area under the stress-strain curve. Examples comparing three engineering alloys are given in Fig. 1. The shortest curve with the smallest area under the curve is a high-strength cast iron, which is nevertheless, comparatively brittle. The next curve is for cast tank armor, a tough heat-treated steel. Austenitic manganese steel

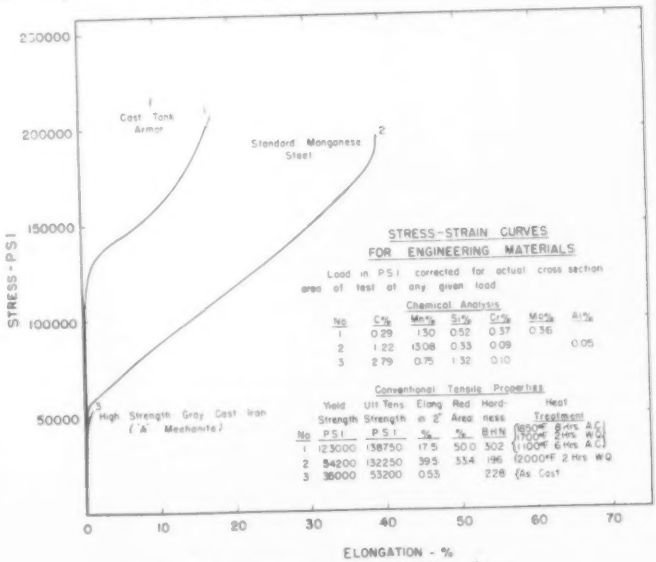

STRESS-STRAIN CURVES
FOR ENGINEERING MATERIALS
Load in P.S.I. corrected for actual cross section area of test at any given load
Chemical Analysis
No C% Mn% Si% Cr% Mo% Al%
1 0.29 1.50 0.52 0.37 0.36
2 1.22 13.08 0.33 0.09 0.05
3 2.79 0.75 1.32 0.10
Conventional Tensile Properties
No Yield Strength P.S.I. Ult. Tens. Strength P.S.I. Elong. in 2" % Red Area % Hardness BHN Heat Treatment
1 123000 138750 17.5 50.0 302 1650°F 8 Hrs. A.C. 1700°F 2 Hrs. W.Q.
2 84200 132250 39.5 33.4 196 1100°F 6 Hrs. A.C.
3 38000 53200 0.55 226 2000°F 2 Hrs. W.Q. As Cast
1 Cast Tank Armor
2 Standard Manganese Steel
3 High Strength Gray Cast Iron ('A' Meehanite)
STRESS - P.S.I.
250000
200000
150000
100000
50000
0
10
20
30
40
50
60
70
ELONGATION - %

Fig. 1 Selected manganese steel data sheets

is perhaps twice as tough, however, because it elongates much more before fracture.

A quick approximate way to make this comparison is to use the product of ultimate tensile strength and tensile elongation, which has been termed the "merit number."[2] For the iron this is 53,200 × 0.53 = 28,200, while for the standard manganese steel it is 132,500 × 39.5 = 5,230,000, which is an impressive difference. Generally such a rating will parallel experience for industrial impact. It is not intended to cover such factors as notch brittleness, low-temperature brittleness and the like, which may be illuminated by special tests.

This merit number provides a useful index of toughness only for simple tension. As the specimen stretches in one direction, it contracts in the other two, with progressive reduction in area. However, if contraction is constrained in one or both of these other directions, the material is more vulnerable to structural damage since the benefits of plasticity are reduced or lost. The result is sudden, brittle failure when the stress goes too high, even if the material is quite tough in uniaxial tension. The ultimate stress under such conditions is variously known as "fracture stress" or "technical cohesive strength."

The notch-bar tests of the Charpy and Izod types are useful for estimating comparative fracture stress values for different materials. However, since we are here concerned chiefly with compressive stresses from impact, we are not assuming that impact toughness or fracture resistance are related under the two conditions. Neither is it necessarily true that the sensitivity of properties to speed of testing or to temperature is related to toughness in compression.

The tendency to fail by a sudden release of the interatomic forces on crystalline planes of a material is generally termed cleavage, in contrast to the slip or plastic flow that characterizes ductile metals. In notch bar tests, brittle or cleavage fractures are observed below a "transition temperature" that is characteristic of a specific material. Thus it is desirable to test for tensile toughness at or below the temperature of service. It is a distinct asset for an alloy to have a transition temperature well into the subzero zone.

Austenitic steels as a class tend to exhibit flow rather than cleavage fracture and since they seem to retain this tough character down to −40° F. or below they provide industrial impact resistance from whatever viewpoint they are evaluated.

A few expensive nonferrous metals approach the toughness of the austenitic steels, but they are seldom competitive as weld deposits because of their high cost. The usual problem posed to the user is how to get the best flow resistance or dimensional stability (through high elastic strength) without running the risk of premature fracture. For base metal selection and tensile stresses, the combination of the merit number and notch bar impact data for the temperature concerned is considered generally satisfactory. For weld

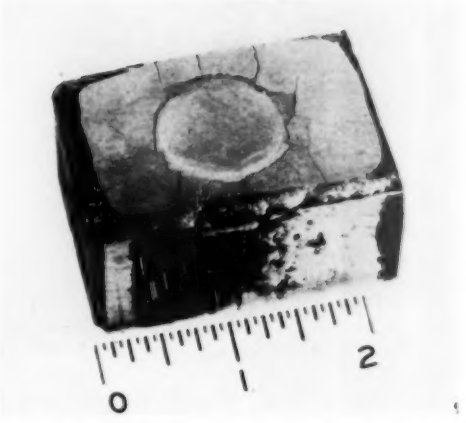


Fig. 2 Gas weld overlay of martensitic iron, 1/16 in. thick, on a 1 1/16-in. thick base of 1020 steel; cracked by one 500 ft.-lb. impact blow delivered on a 7/8-in. diameter area. Cracking of the hard facing is due primarily to plastic deformation of the soft base

overlays stressed chiefly by compression from impact the pattern may be different.

In this paper austenitic manganese steel is used as the tough extreme of a trio of preferred materials. The brittle extreme is martensitic iron, described later in more detail. The latter is too brittle in tension to provide useful data in a Charpy or Izod test. Substituting a 3/4 in. square bar, unnotched, for the 0.394 in. square notched Charpy specimens, and breaking it with the same equipment yields values of about 10 to 15 ft.-lb. This brittleness can be confirmed qualitatively by drop hammer impact. Figure 2 shows a specimen of SAE 1020 steel hard faced with a thin layer of martensitic iron. The hammer has struck the face on a 7/8 in. diameter area with a 500 ft.-lb. blow and caused tensile failure of the deposit, chiefly because the soft base has deformed, thereby depriving the deposit of adequate support.

Compare this with Fig. 3, which is a smaller base surfaced with martensitic iron in a 1/16-in. deep recess on the top surface. The base is a considerably stronger steel. This specimen has withstood 100 similar 500 ft.-lb. blows for a total of 50,000 ft.-lb. without failure and with negligible deformation. Practically, the reaction to Fig. 2 is "brittle" while to Fig. 3 it is "tough." Design and base material selection account for the difference, since the same hard-facing material was used. The mechanics of the "tough" assembly permit exploiting the wear and abrasion resistance of the overlay while those of Fig. 2 do not.

IMPACT STRESSES

Some insight concerning stresses from impact is desirable, even if precise calculations cannot be made. The different behavior in Figs. 2 and 3 requires logical explanation so that it may be translated into practical terms.

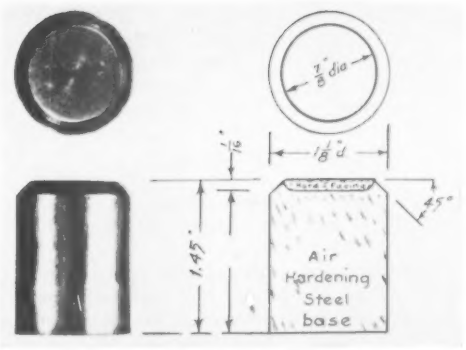


Fig. 3 Gas weld overlay of the same martensitic iron 1/16 in. thick, on a 1 7/16-in. high cylinder of air-hardening steel. This specimen took one hundred 500 ft.-lb. blows on its 1 7/8-in. diameter top surface with no sign of damage

It is easy to make qualitative recommendations for impact service. Essentially they repeat a pattern that is based on field experience: use martensitic irons for light impact, martensitic or pearlitic steels for medium impact and austenitic manganese steels for heavy impact. Substitutions for these three can be satisfactorily made if the essential properties that confer impact resistance are maintained at the same level by the substitute.

The difficulty appears when a specific application must be typed, particularly if service conditions are not obviously at one extreme of the scale. The exploratory work presented here will attempt to reduce this problem partially to quantitative terms that will help in selection.

The problem can be defined partly by classifying light impact in the range that will not produce compressive stresses on the receiving face in excess of the yield strength. This will cover the field where the stresses are entirely elastic and structural damage is not likely. Obviously the avoidance of plastic deformation depends on the strength of the face struck. It is feasible to employ hard-facing alloys characterized by compressive yield strength values above 150,000 psi. that have good abrasion resistance and other valuable properties. Most of the properly balanced martensitic irons are stronger than this, and therefore the 150,000 psi. figure is arbitrarily selected as the upper limit for stresses produced by light impact.

Since the velocity of the impact blow is also critical it appears necessary to restrict light impact to velocities below about 85 ft. per second, assuming an elastic modulus of 29 million psi.

The limit of medium impact is harder to define. The term may be applied to the range where deformation is expected but where failure from deformation is unlikely. Since the kind of structural damage involved is cumulative, somewhat like fatigue, time or cycles of repetition become important. Here, again, the definition depends on properties of materials. Perhaps an impact value of around 400 ft.-lb. received on an 0.875-in. diameter face (0.60 sq. in.) can be used as an arbitrary limit. The stresses resulting from such blows will soon fracture the hard irons, but martensitic steels have absorbed them while slowly deforming. Indefinite life is not expected, however.

Similar blows will deform austenitic manganese steel more rapidly, but the work hardening that results gradually builds up resistance to flow. Eventually the point is reached where the blow is absorbed elastically, while similar pounding of a martensitic steel continues to produce deformation.

Heavy impact can then be assigned the range above 400 ft.-lb. blows, which is typically seen in railway trackwork and heavy crusher operation. Some calculation of trackwork and wheel loads indicate that freight car wheel impact may reach 33,000 ft.-lb.

Kinetic energy, referring to a body in motion, is usually expressed in foot-pounds. A 10-lb. weight that has freely fallen for 50 ft. has 10×50 or 500 ft.-lb. of kinetic energy. Similarly, a 50-lb. weight at the end of a 10-ft. drop can deliver a 500 ft.-lb. blow. Kinetic energy is also calculated from the weight and the velocity of a moving body.

The deformation of a body struck by a blow may be either elastic or a combination of elastic and plastic. This is also true for static loads. The elastic deformation occurs first, and if the energy of the blow is not exhausted when the elastic limit of the metal is reached, the remaining energy causes permanent plastic deformation. In contrast, the elastic deformation recovers promptly and returns its energy to the striking object as rebound.

Recognition that energy absorbed elastically is returned to the striking object as rebound without damage to the object struck is important. If the elastic limit of a material is high enough in relation to the energy of the blow, it can absorb any number of such blows without harm. Impact damage occurs when the stresses are above the elastic limit and some permanent deformation occurs. Impact resistance in a metal is then a combination of two factors: elastic strength and ability to undergo plastic deformation without failure. Hard, brittle alloys tend to have the first, while ductile materials like soft steel may have the second. A few materials, like austenitic manganese steel, may have a unique combination of both under certain circumstances.

The merit of hard brittle alloys with high elastic strength is evidenced by the every-day performance of chilled cast-iron freight car wheels. The tread surface of these is a brittle white iron that can stand only about 2.0% plastic deformation in compression without failures. The impact blow at the tread surface when a loaded freight car goes over a bumpy railway crossing may range from 1115 to 33,300 ft.-lb.,[3] yet thousands of such blows are absorbed elastically and returned as rebound month after month in normal freight car operation. Tread failures from impact are rare.

Part of this excellent performance is the result of design. The load is spread over a relatively flat surface in such a way that the stress is compressive. The area taking the stress is well supported below and on the sides and unfavorable stress concentrations that might exceed the elastic limit are avoided. Also the wheel-rail zone is cushioned appreciably by the tie and roadbed structure.

Another good example of elastic strength was observed during a study of Ni-Hard grinding balls some years ago. Suspecting that brittleness might be a liability in ball mills, a crude test of 3-in. diameter balls was arranged by dropping the balls from a second story window onto a flat hard anvil set in the ground. The balls would rebound from the anvil back up to the second story and could be caught by the person who dropped them.

The ball surfaces were not perfect, however, and eventually an unfavorable stress concentration would cause a crack. A ball would usually fail soon thereafter though it had undergone many previous drops without apparent damage.

Brittle materials are very vulnerable to tensile stresses. It is an important rule in design to avoid stressing a brittle member in tension. To follow this principle in hard facing requires several precautions.

A hard overlay usually is intended to take impact in compression, as a blow on a flat face. However, visualize the overlay in place without the support of the base metal underneath the point of the blow. The overlay will be deflected and the face away from the blow will be stressed in tension. This can make it crack at the point under the blow.

Also visualize an overlay extending out to the edge of a part and receiving a blow without the support of the base. This also can cause cracking where the bending of the overlay causes tensile stresses.

Of course, the overlay is supported by the base in use. But, unless the base is as strong as the hard facing, it may not be a competent support. The stress of an impact blow may be transmitted through the overlay and deform the soft base. Where such underbead deformation occurs, the weld receives only partial support, and it may crack in the same pattern that would appear if there were no support. Even if it escapes cracking, the residual tensile stress will increase vulnerability to the next blow.

Thus, thin overlays and those extending to an edge may crack in tension from impact where under more favorable circumstances they would receive chiefly compressive stresses and would escape damage.

Aside from the matter of support, the lateral flow of a soft material leads to tensile stresses on its upper and lower surfaces. Weld junctions at these locations may be cracked in tension.

In dealing with impact in this manner several questions develop. They relate to the magnitude of stresses that impact blows develop, their distribution below the surface, the compression properties of the alloys used and ranking of hard-facing alloys under impact compression. These were partially answered by a combination of calculations and experiments.

Impact stresses result when a body in motion is arrested by collision. The kinetic energy of the moving body is converted to either potential energy that is stored as elastic deformation, to heat that is associated with plastic deformation, to sound and perhaps also to less obvious phenomena. If the colliding surfaces are essentially elastic the kinetic energy of a direct central blow falls to zero and then returns as rebound in the opposite direction.

The recovery of kinetic energy is not complete. Some quality of the colliding materials prevents attaining the original velocity of the moving object. The ratio of velocity after the blow to that before is called the coefficient of restitution; for an inelastic body it is zero, while values near one characterize materials that approach perfect elasticity. A high value for this coefficient, under suitable conditions of light impact, means that little or no structural damage to the body results. Billiard balls do not ordinarily fail in use, though they receive innumerable impact blows. (The coefficient of restitution for ivory is about 8/9.)

This pattern is important for engineering. If a highly elastic material is selected for impact service and the stress level from impact is kept below the elastic limit of the material, structural damage is not expected and a long useful life is normal. Controlled

Table 1—Impact Formulas

Kinetic energy: K. E. $= W \cdot h$ or $\frac{W \cdot v^2}{2g}$

Velocity: $v = g \cdot t = \sqrt{2g \cdot h}$

Time: $t = \frac{v}{g} = \sqrt{\frac{2h}{g}}$

Work: Kinetic energy equivalent $= \frac{Pm \cdot dm}{2}$

Maximum force (static load equivalent): $Pm = \frac{A \cdot dm \cdot E}{L}$

Maximum deformation: $dm = \sqrt{\frac{2 \cdot W \cdot h \cdot L}{A \cdot E}}$

Maximum stress: $\sigma = \frac{Pm}{A} = \sqrt{\frac{2 \cdot E \cdot W \cdot h}{A \cdot L}}$

Maximum surface elastic stress: $\sigma_o = E \frac{v}{c}$

Symbols and Units

W = weight, lb.
h = height of free fall, in.
v = velocity, ft. per second
t = time of free fall, sec.
A = area of specimen, sq. in.
L = length of specimen, in.
P = load, lb.
Pm = maximum equivalent static load, lb.
dm = maximum deformation, in.
σ = unit stress, psi.
g = acceleration due to gravity = 32.2 ft. per sec. per sec.
δ = unit deformation, in. per in.

E = elastic modulus $= \frac{\sigma}{\delta} = \frac{P \cdot L}{A \cdot d}$

c = velocity of stress wave $= \sqrt{\frac{E}{\text{Mass per unit volume}}} =$ 16,800 ft./sec. for steel

cyclic impact, as in a tappet, can thus be handled through proper design and material selection.

The difficult part of this design and selection problem is determination of the stresses expected. Without an estimate of these, selection of the optimum material becomes guesswork.

The energy of an impact blow is found by well-known formulas, detailed in Table 1. In ballistics the energy is ordinarily calculated from velocity and weight. Laboratory tests generally involve a known drop that produces impact and the energy is found as the simple product of weight × height, expressed as foot-pounds or inch-pounds.

The formulas for calculating stresses in a body subjected to impact are less well known and must be used with greater caution. Some are detailed on in Table 1 for the condition sketched in Figure 4. Tables 2 and 3 contain results of calculation on laboratory test specimens.

These formulas are based on the assumption of elastic behavior. When plastic flow also occurs the behavior is complicated. Stresses will, of course, rise to the elastic limit, but this is not a fixed point for work-hardenable materials. The yield strength of manganese steel rises with each blow until it is high enough to provide elastic behavior. This can be demonstrated in tension, compression or impact. The flattening of a flow curve in Fig. 5 is evidence of this.

Despite available formulas and the assumption of elasticity the stresses from impact are not easy to calculate precisely. Much of the difficulty comes from inability to identify exactly the portion of the body struck that responds to the blow. Even in the case of a small test specimen with simple and precise geometry, we cannot be sure of transferring all of the impact energy solely into the test piece; it must be supported upon an anvil that can share in energy absorption.

Table 2—Calculated Maximum Impact Stresses in Elastic 1.0 In. Diameter by 1.0 In. High Steel Specimen

$$\text{Maximum equivalent static load} = \frac{\text{Cross-sectional area} \times \text{max. deformation} \times \text{elastic modulus}}{\text{Length of specimen}}$$

$$\text{Maximum compressive deformation} = \frac{Wh \cdot 2}{Pm} = \sqrt{\frac{2 \cdot Wh \cdot L}{AE}}$$

$$Pm = \frac{A \cdot dm \cdot E}{L} = A \cdot \frac{Wh \cdot 2}{Pm} \cdot \frac{E}{L}$$

$$(Pm)^2 = \frac{A \cdot Wh \cdot 2 \cdot E}{L} = \frac{0.7854 \text{ sq. in.} \times Wh \times 2 \times 30 \times 10^6 \text{ psi.}}{1.000 \text{ in.}}$$

$$Pm = \sqrt{0.7854 \times 60 \times 10^6 \times Wh} = 10^3\sqrt{47.1 \text{ lb./in.} \times Wh} = 6860\sqrt{Wh}$$

Height of drop (h), in.*	*Energy* (Wh), in.-lb.*	$\sqrt{Wh}$	*Deformation (dm), in.*	*Max. load (Pm), lb.*	*Maximum stress σ psi.*
120	6000	77.5	0.0226	531,000	676,000
60	3000	54.8	0.0160	376,000	478,000
30	1500	38.7	0.0113	266,000	338,000
10	500	22.4	0.0065	154,000	196,000
2	100	10.0	0.0029	68,600	87,000
1	50	7.1	0.0021	48,500	62,000

* For a 50-lb. hammer, as shown in Fig. 11.

These calculations assume a completely rigid anvil and tup, and no plastic deformation; stresses are higher than actual values, which will depend on the proportion of the impact energy absorbed by the supporting structure.

Table 3—Calculated Maximum Impact Stresses in Elastic 0.875 In. Diameter by 0.875-In. High-Steel Specimen as Used for Tests Shown in Fig. 13

$$\text{Maximum equivalent static load} = \frac{\text{Cross-sectional area} \times \text{max. deformation} \times \text{elastic modulus}}{\text{Length of Specimen}}$$

$$\text{Maximum compressive deformation} = \frac{Wh \cdot 2}{Pm} = \sqrt{\frac{2 \cdot Wh \cdot L}{AE}} = \sqrt{\frac{2Wh \cdot 0.875}{0.6013 \times 30 \times 60^6}} = \frac{0.31\sqrt{Wh}}{1000}$$

$$Pm = \frac{A \cdot dm \cdot E}{L} = A \cdot \frac{Wh \cdot 2}{Pm} \cdot \frac{E}{L}$$

$$(Pm)^2 = \frac{A \cdot Wh \cdot 2 \cdot E}{L} = \frac{0.6013 \text{ sq. in.} \times 2 \times 30 \times 10^6 \text{ psi.}}{0.875 \text{ in.}} \times Wh = 41.2 \times 10^6 \frac{\text{lb.}}{\text{in.}} \times Wh$$

$$Pm = \sqrt{0.6013 \times 60 \times 10^6 \times Wh} = 10^3\sqrt{41.2 \text{ lb./in.} \times Wh} = 6410\sqrt{Wh} \text{ lb.}$$

Height of drop (h), in.*	*Energy* (Wh), in.-lb.*	$\sqrt{Wh}$	*Deformation (dm), in.*	*Max. load (Pm), lb.*	*Max. stress (σ), psi.*
120	6000	77.5	0.024	496,000	825,000
60	3000	54.8	0.017	351,000	583,000
30	1500	38.7	0.012	248,000	412,000
10	500	22.4	0.007	143,500	238,000
2	100	10.0	0.003	64,100	106,500
1	50	7.1	0.002	45,400	75,000

* For a 50-lb. hammer as shown in Fig. 11.

These calculations assume a completely rigid anvil and tup, with no plastic deformation of the specimen. The indicated stresses are higher than actual values, which will depend on the portion of the impact energy absorbed by the supporting structure. The apparatus in Fig. 11 seems to absorb over 90% of the impact energy in its structure and foundation. Industrial equipment may be similarly resilient.

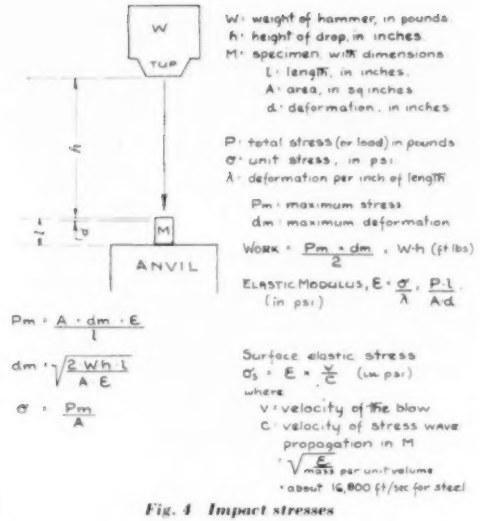

W
TUP
ANVIL
M
W: weight of hammer, in pounds
h: height of drop, in inches
M: specimen, with dimensions
l: length, in inches,
A: area, in sq inches
d: deformation, in inches
P: total stress (or load) in pounds
σ: unit stress, in psi
λ: deformation per inch of length
Pm: maximum stress
dm: maximum deformation
WORK = Pm · dm / 2 , W·h (ft lbs)
ELASTIC MODULUS, E = σ/λ , P·l/A·d (in psi)
Pm = A · dm · E / l
dm = √(2 W·h·l / A E)
σ = Pm / A
Surface elastic stress
σs = E × V/C (in psi)
where
V: velocity of the blow
C: velocity of stress wave propagation in M
= √(E / mass per unit volume)
= about 16,800 ft/sec for steel

Fig. 4 Impact stresses

A massive and rigid anvil is helpful in controlled impact tests. It can be designed to absorb without rebound only a small proportion of the impact blow and thus influence test results only slightly, but even under good conditions the effect is finite. If the anvil structure is springy or has a strong damping action the effect on impact stress can be quite large.

This has the practical effect of reducing stress in the test specimen and making calculated values too high by some unknown factor. This may not be serious during a test campaign provided the same apparatus and conditions are maintained throughout. For the practicing engineer the anvil behavior provides a factor of safety.

Calculated stresses given herein must be accepted on this basis. They may be comparative, but they are not represented as absolute.

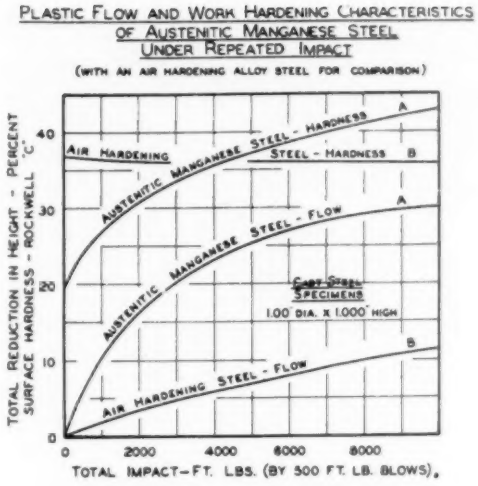
A possible exception is provided by high velocity impact. Timoshenko[1] provides the formula:

Compressive stress =

$$\text{Elastic modulus} \times \frac{\text{Velocity of the blow}}{\text{Velocity of stress wave in the body struck}}$$

Note that only the velocity, rather than the energy, of the blow is involved. At the surface struck stress will rise to a high value if the stress wave that takes energy from this surface moves away slowly in relation to the external impact velocity. Thus even a light blow, if of high velocity, can cause plastic deformation if elastic strength is too low. The result will be upsetting of the surface struck. Figure 6 is a nomograph indicating the relation between velocity and the elastic limit of steel that is required to avoid plastic flow, as calculated from the formula above.

Since the maximum elastic limit or yield strength that we have encountered in hard-facing alloys is about



	Chemical Analysis						Heat Treatment	
Heat No.	C%	Mn%	Si%	Ni%	Cr%	Mo%	°F	Cooling
A	1.17	12.8	.46				1850	Water Quench
B	.74	.88	.30	75	1.40	.38	1650–Air + 1200–Air	

Figure 5

350,000 psi., the maximum impact velocity that can be taken without plastic flow appears to be below 200 ft. per second. Even this assumption is based on the probability that slight deformation will initially occur and by work hardening raise the proportional limit to the arbitrary yield strength.

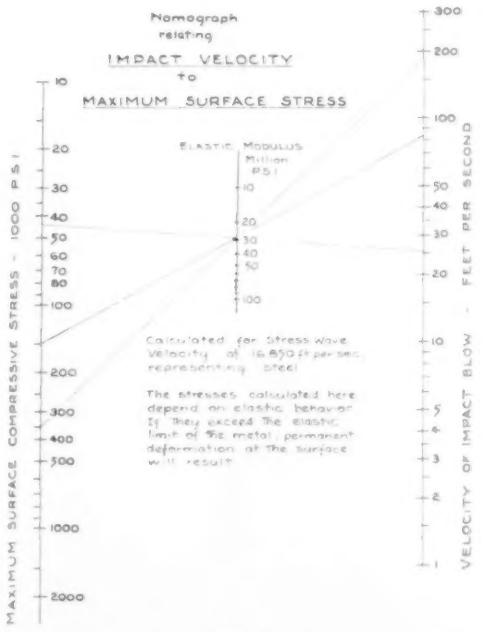

Nomograph relating
IMPACT VELOCITY
to
MAXIMUM SURFACE STRESS
MAXIMUM SURFACE COMPRESSIVE STRESS - 1000 P.S.I.
ELASTIC MODULUS Million P.S.I.
VELOCITY OF IMPACT BLOW - FEET PER SECOND
Calculated for stress wave velocity of 16,850 ft per sec. representing steel
The stresses calculated here depend on elastic behavior. If they exceed the elastic limit of the metal, permanent deformation at the surface will result

Figure 6

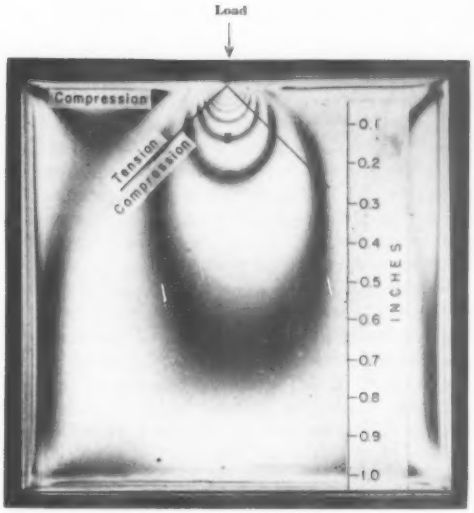


Fig. 7 Photoelastic stress pattern of a concentrated load on a flat face. This loading is frequent in hard facing applications. Counting from below, each oval fringe crossed indicates another multiple of the stress represented by the innermost fringe. The darkening of the fringes along 45-degree lines from the surface indicates the location of isoclinic lines that follow the boundary between tension and compression

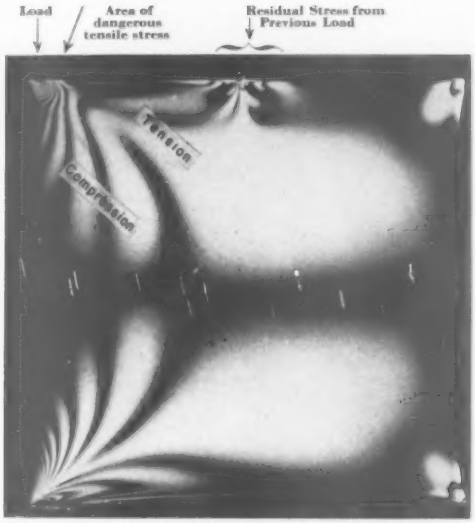


Fig. 8 Photoelastic stress pattern of a concentrated load near an edge. This is the situation that may cause spalling of brittle hard-facing alloys. This specimen was previously loaded beyond the elastic limit in the center of the top face, leaving residual stresses

Fortunately most industrial impact blows are below this velocity. By taking an arbitrary limit of 150,000 psi. as the maximum allowable stress for hard, martensitic alloys a maximum velocity of about 85 ft. per second (or 60 mph.) is indicated. These values are suggested as the upper limit of light impact, since by designing below them it should be possible to avoid plastic deformation and obtain very long useful life.

The distribution of stresses below the surface has been examined by photoelastic methods. Figure 7 shows a concentrated load on a flat face, representing many hard-facing conditions. Note that the stress lines are very close near the point of loading, thus indicating high stresses. Moving from below toward the load application point, each fringe crossed provides a multiple of the stress represented by the innermost fringe. Thus in this model the stress is four times as great 0.11 in. below the surface as it is near the center of the model 0.7 in. from the surface. Just under the load it is perhaps 18 times as great as at the center.

Practically this means that maximum protection is needed at and near the surface. Since the load is spread over more metal as distance from the loading point increases, it is possible to cope with the stress elastically with lower strength alloys than are required at the surface. The merit of hard facing, provided the overlay has high elastic strength, is therefore obvious. Note, however, that a very thin overlay would not prevent high stresses in the base. Some thickness is necessary to make a considerable reduction in underbead unit stresses. Thus carburizing, nitriding and other thin, hard layers are unlikely to provide the same type of protection against concentrated compression.

If the load is applied near an unsupported edge, as in Fig. 8, the material under the load is in compression, but on the top surface, to the right of the isoclinic dividing line, tension prevails. Since very hard alloys have a low effective tensile strength, this introduces the danger that an overlay may fail in the tensile zone where stress and stress gradients are high, as evidenced by closely spaced fringes in the figure. This of course confirms experience; the brittle irons are not recommended for edge impact. Generally the best material is a martensitic steel, which is tougher, but in some applications where heat is involved, it may be desirable to substitute hot hardness and creep resistance for cold hardness and high yield strength. Hot shear knives are an example. They may be faced with cobalt or nickel base alloys to provide hot strength, but this advantage is not likely to appear over a high-speed martensitic steel unless the actual edge working temperature is above 1100° F.

Excessive impact stresses near an edge characteristically spall off the edge with shear failures. When such chipping is only incidental to carrying the main load on a flat face, the ragged fracture edges can generally be avoided by chamfering the edge, as in Fig. 9. This puts the undesirable tensile and shear stresses in a region of better support.

There remains the question of how thick an overlay should be to put the weld junction below the region of high stresses. This has no simple answer since the

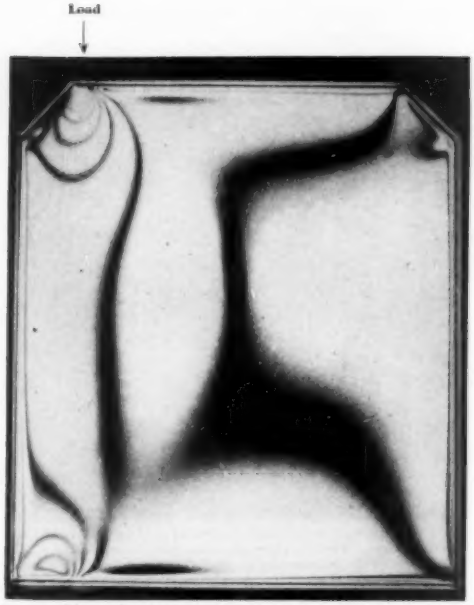


Fig. 9 Photoelastic stress pattern of a concentrated load near a chamfered edge. This design is desirable if impact is likely to stress the edge of a brittle hard-facing overlay

relative strength of the overlay and base as well as the geometry and impact magnitude are involved. Probably a simple concept of assuming that stresses spread at a 45-degree angle from the point of impact combined with designing unit stress areas on a basis of yield strength ratios is a satisfactory approximation. Thus if a blow is delivered to a 0.60-sq. in. face of a 150,000 psi. yield strength hard facing on a soft steel whose yield strength is 50,000 psi., the area at the weld junction should be 150,000/50,000 × 0.60 sq. in. or 1.8 sq. in. For circular areas this requires a diameter of 1.5 in. at the weld juncture which in turn would need an overlay 9/32 in. thick if the 45-degree rule is used.

Large areas receiving the impact require thicker overlays. Sharp point impact, like that of sharp rocks on digging equipment, may require only a thin layer to spread the stress to the point of preventing base metal flow (Fig.7). On the other hand severe abrasion may motivate thick deposits. There are practical limits that may prevail, since hard facings are generally confined to the range of 1/16 to 3/8 in., with 1/8 to 3/16 in. as the usual practice.

These calculations are not presented as rigorous but they may be helpful in estimating the suitability of a proposed overlay. The discussion assumes that impact will be light enough to avoid plastic flow of the hard facing. If not, calculations become difficult and hardness probably will have to be sacrificed for toughness. This is the point where the term medium impact becomes appropriate.

COMPRESSIVE STRENGTH

To supply quantitative data that seem to be lacking in engineering literature, a number of tests have been made on hard-facing alloys. The list is by no means comprehensive, but has been picked to cover the more important types as described later under alloy selection. The specimens were cut from small castings, machine ground to cylinders from 1.0 to 2.0 in. long and from 0.37 to 0.40 in. diameter. SR-4 strain gages cemented to their sides provided stress-strain data while under compression in a universal testing machine.

A series of tests comparing 1.0, 1.5 and 2.0-in. long specimens did not give a consistent pattern of either strength or ductility versus length, so the variations noted were considered in the general area of experimental error. However, for ductile materials the long specimens may give trouble from buckling. For short specimens end effects are a problem if rigorous analysis of the data are planned. This program is continuing and may latter be the subject of another report. Meanwhile the data are summarized in Fig. 10 as being pertinent to the present discussion.

The values for yield strength and plastic deformation are most useful. The yield strength is based on 0.1% set as determined from the stress-strain curves (this is only half the arbitrary set generally reported for tensile tests). The yield for 0.05% set is as a rule appreciably lower for most hard-facing alloys, but since they generally carry retained austenite which can transform under impact, thereby raising the yield strength, the 0.1% value was selected as more representative.

Even the brittle materials exhibit a small amount of plastic deformation before failure that is many times the amount that would be observed in tension. This is one reason why the merit of hard facings should not be judged from conventional tensile tests. The total deformation is the sum of elastic and plastic compression; for the very high strength types the elastic portion may overshadow the plastic deformation.

Castings rather than weld deposits were selected for several reasons. If the casting is small it cools with rates in the same range as weld deposits. It has the added advantage of lacking the nonrepresentative multiple bead structure of welds, which for these specimens would have had to be much different than the average overlay. Welds of this thickness would also run the risks of internal defects and cracks, since it is seldom practicable to deposit over 3/8 of an inch of brittle hard facing without cracks. In thick layers the problem of thermal stresses is acute. A final advantage is the stability of composition. Gas welds tend to pick up carbon while arc welds lose it as well as other elements. Castings are thus intermediate between the two and moreover provide assurance that replicate samples do not vary in composition.

REPEAT IMPACT TESTING

The compression data in Fig. 10 and the stress pat-

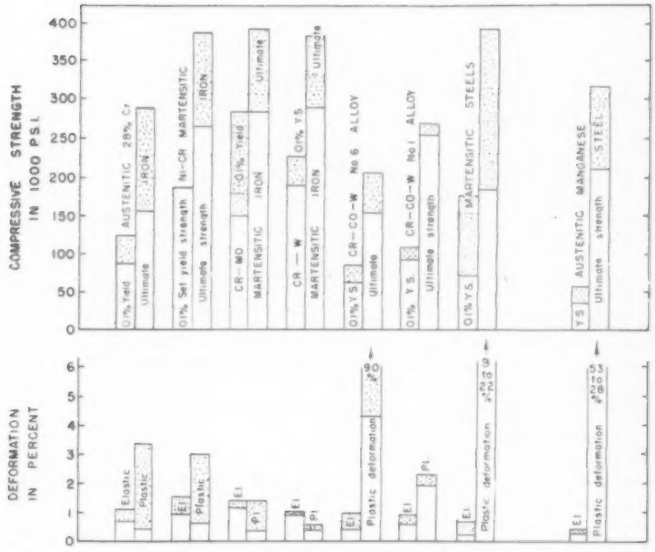


Fig. 10 Comparative compression properties of various hardsurfacing alloys. Stippled areas define observed scatter bands

Fig. 11 Repeat impact testing machine. With it a 50-lb. hammer can be dropped from any height up to 10 ft., providing a range from 0 to 500 ft.-lb. as impact energy

terns in Figs. 7, 8 and 9 are from static tests. A further ranking of the important hard-facing alloys under dynamic compression or actual impact was sought. The method selected is a modification of that used to study the behavior of manganese steel track work compositions.

The apparatus is a drop hammer (Fig. 11) permitting a 50-lb. weight to fall through any height up to 10 ft. A $1^1/_2$-diameter tup of hardened steel, which is part of the hammer, strikes any specimen that is positioned on the 2-in. diameter anvil (Fig. 12). The hammer is guided by steel ways, which also catch the hammer on the rebound by means of a latch plate. The foundation is heavy but not absolutely rigid, making it difficult to state how much of the blow is absorbed by the machine. This makes the test conditions arbitrary, and confines their usefulness largely to comparisons of materials tested on the same equipment.

The full 500 ft.-lb. blow is too severe for some materials so a graduated sequence, as detailed in Table 4, was adopted for standardization. After a total of 3000 ft.-lb. the full 500 ft.-lb. blow is continued until impending failure is observed or an arbitrary limit is reached. For Fig. 13 this limit was 50,000 ft.-lb. as a cumulative total.

The tests of hard-facing alloys were applied to $^7/_8$ in. diameter by $^7/_8$-in. high specimens, machine ground from small castings. The structures tested are those produced by mold cooling and are thus comparable to those produced by slowly cooled weld deposits rather than to the hardened and tempered structures that represent heat-treated steels. Figure 13 is a graphic summary of the behavior, exhibiting both flow resistance and crack resistance in one chart. Reproduc-

Fig. 12 Anvil and specimen as employed for repeat impact tests

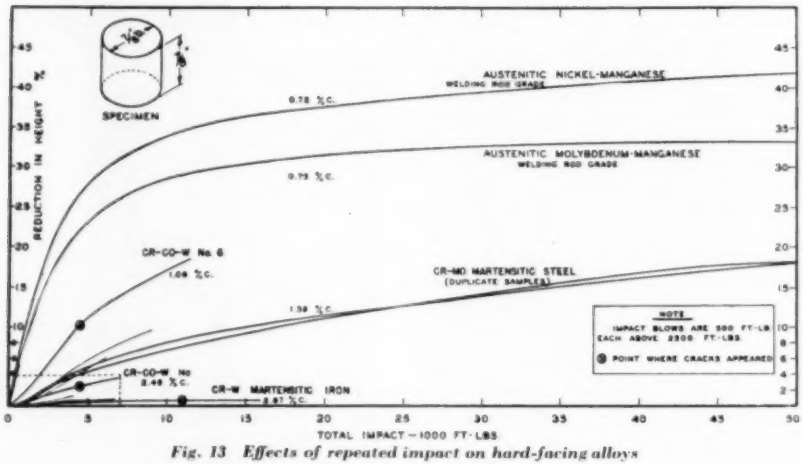


Fig. 13 Effects of repeated impact on hard-facing alloys

tibility of the technique is indicated on the graph by the duplicate tests on the martensitic steel. Note that the austenitic manganese steels and the martensitic steel went to the limit without failure, though had the test been continued the hard steel would be expected to fail first. The alloys with marked flow resistance are susceptible to failure after the 500 ft.-lb. blow was reached, though some of these withstood a number of blows before cracking.

In the lower corner of Fig. 13 are several materials with a short life expectancy under the full blow. These are detailed in Table 5, with the austenitic nickel-manganese steel for comparison.

The stresses that impact would develop in a fully elastic specimen of this size (7/8 in. diameter by 7/8 in. high) if struck on a completely rigid anvil were calculated in Table 3. By observation of the hammer rebound it seemed unlikely that more than about one-tenth of the full 500 ft.-lb. was returned by elastic behavior, the balance probably going into plastic deformation of the specimen or into displacement of the anvil and its foundations.

To confirm this, SR-4 strain gages were attached to a 1 1/8 in. diameter by 1.45-in. high specimen as in Fig. 14, and the instantaneous maximum deformation (*dm* of the formula in Fig. 4) measured by photographing the trace of a cathode-ray oscilloscope. The specimen selected carried a 1/8-in. thick weld deposit of a Cr-Mo martensitic iron on a base of air-hardening steel, and had demonstrated substantially elastic behavior under 500 ft.-lb. blows. It had previously received 50,000 ft.-lb. of total impact with a reduction in height of only 0.12% (0.0031 in.). The oscilliscope evidence indicated a maximum defor-

Table 4—Standardized Sequence of Impact Blows

(50-lb. drop hammer; 1 1/2 in. diameter tup; 2 in. diameter anvil)

Blow:	*1*	*2*	*3*	*4*	*5*	*6*	*7*	*8*	*9*	*10*	*11*	*12*	*13*	*Thereafter*
Height, ft.:	0.6	1.0	1.6	2.0	2.6	3.2	4.0	5.0	6.0	7.0	8.0	9.0	10.0	10.0
Ft.-lb.:	30	50	80	100	130	160	200	250	300	350	400	450	500	500
Cumulative total, ft.-lb:	30	80	160	260	390	550	750	1000	1300	1650	2050	2500	3000	3500 etc.

After the 13th blow, if the specimen has not broken, the 500 ft.-lb. blows are continued to the point of impending failure or to a total of 50,000 ft.-lb.

Table 5—Effects of Repeated Impact on Hard Facing Alloys

Type	*Nominal composition, %* C	Mo	Cr	Co	W	*Hardness, Rockwell*	*Total cumulative impact To crack*	*To fail*	*Reduction in height, %*
High-Chromium iron	5.00	..	30	..	..	C50.0	1000	1650	0.52
Martensitic iron	3.52	4	4	..	..	C52.0	2500	6000	6.21
Martensitic iron	3.76	..	3	(3% Ni)		C48.7	2500	4000	1.19
Cobalt base	2.46	..	31	43	14	C50.2	4500	7000	3.61
Cobalt base	1.04	..	27	63	5	C41.0	4500	11500	18.46
Austenitic Mn steel	0.72	(13% Mn, 3.7% Ni)				C 4.7	(No cracks at 50,000 ft.-lbs.)		41.92

Fig. 14 Specimen as used with SR-4 strain gages to determine impact deformation. The surface is protected with a $^1/_8$-in. thick gas weld of a Cr-Mo martensitic iron, and withstood one hundred 500 ft.-lb. blows without damage. The oscilloscope indicated 0.005-in. max. deformation under a 500 ft.-lb. blow, which indicates a maximum surface stress of 171,000 psi. (See also Fig. 3)

mation of about 0.005 in., instead of the theoretical 0.024 in. The calculated maximum stress (or load) then is

$$Pm = \frac{\left\{\begin{matrix}\text{Area of}\\ \text{specimen}\\ \text{body}\end{matrix}\right\} \times \left\{\begin{matrix}\text{maximum}\\ \text{deformation}\end{matrix}\right\} \times \left\{\begin{matrix}\text{elastic}\\ \text{modulus}\end{matrix}\right\}}{\text{Specimen length}}$$

Diameter of body = $1^1/_8$ in. = 1.125 in.
Body area = 0.9940 sq. in.
Surface area = 0.6103 sq. in.

$$Pm = \frac{0.994 \text{ in.}^2 \times 0.005 \text{ in.} \times 30 \times 10^6 \text{ psi.}}{1.45 \text{ in.}}$$

$$Pm = 103{,}000 \text{ lb.}$$

σ = maximum surface stress

$$\sigma = \frac{Pm}{\text{Area}} = \frac{103{,}000 \text{ lbs}}{0.6103 \text{ sq. in.}} = 171{,}000 \text{ psi.}$$

The martensitic iron overlay has withstood this stress repeatedly without apparent damage.

It is significant that the composite specimen of Fig. 14 permits the hard facing to receive much more impact on a face of the same area than the standard $^7/_8$-in. diameter test, without signs of cracking. The geometry of the specimen was calculated to provide unit stresses on the top surface equivalent to those in Table 3, though the body stresses would be lower. Here competent support of the overlay is an important asset.

It is not always practicable to support the overlay so that no deformation can occur, especially if the impact blows are heavy. The zone between light impact (with elastic behavior) and very heavy impact where manganese steel is obviously required, has proved hardest to define in engineering terms. From experience with the test described here, an impact value of between 250 and 400 ft.-lb. is required to crack an arc weld overlay of martensitic steel with chamfered edges on a 1.2 in. high by 2.0-in. diameter base of 1045 steel. The top surface struck by the hammer was $1^1/_2$ in. diameter. Six 400 ft.-lb. blows produced a small crack, while twenty-six 250-ft.-lb. blows were required for cracking a companion specimen. The cracks that appeared were associated with small weld defects. Perhaps it would be more realistic to use only the categories of light and heavy impact, since the dividing line can then be set at 150,000 psi. compressive surface stress. The objection to this is the failure to clarify where martensitic steels are the optimum overlay alloys. From Fig. 13, the performance of this structure is clearly intermediate between manganese steel and the hard, brittle alloys.

The suggestion is offered that crack tolerance be used to define heavy impact conditions as those where surface integrity is important. If cracks are not obviously harmful and martensitic steels, even if overstressed, can give reasonable service, they will lend better dimensional stability than the tougher austenitic alloys. Experience permits this kind of decision, but the objective here, which admittedly has not been reached, was to provide a useful rule to substitute for experience. However, the data assembled will provide some guidance, at least, and should permit planning field comparisons with some insight.

HARD-FACING ALLOY SELECTION

The foregoing data lead to the conclusion that the base material, the hard-facing alloy and design are all important matters. The engineer will wish to have ample ductility in the base to protect against sudden fracture, and must therefore compromise with the desirability of high-yield strength, which tends to minimize tensile cracking of the overlay.

A few air-hardening steels will be quite strong after exposure to welding heat, but most of the alloy steels will soften during the hard-facing operation. The benefit of a hardened steel is thereby lost unless heat treatment after welding is included. This is usually an undesirable complication, though it may be standard practice for critical structural members like oil well tool joints.

For a simple operation the best base selection is usually an unalloyed carbon steel. The practical range extends from SAE 1020 up to 1095, though welding difficulties increase with carbon content. A favorable base, combining good weldability and strength after welding is SAE 1045 steel (Table 6). If the steel is clean and well killed, welding difficulties are minimized.

The new ductile irons, characterized by the easy castability of gray iron, a yield strength range from 45,000 to 80,000 psi., ferritic, pearlitic or austenitic structures as specified, and from five to twenty times the ductility of gray iron, offer attractive possibilities to the engineer designing machine parts that may be subjected to light or medium impact. Hard facing can protect them from surface wear that would otherwise be too severe.

In the field of heavy impact, common to the mining, construction, quarrying, railway, earth-moving and

Table 6—Properties of SAE 1045 Steel

	Yield Strength, psi.	*Ulimate Strength, psi.*	*Elong. in 2 in., %*	*Red. area, %*	*Hardness, Rc*	*Hardness, Bhn.*	*Heat treatment, ° F., Quench*
Natural hot rolled	48,500	90,000	19	42	9	180	...
Cold drawn	87,000	105,000	14	40	14	207	...
Quenched and drawn at 1000° F.	71,000	106,000	22	52	18	220	1500—Water
(Above data from Ryerson data sheet)							
As-rolled, at 0.40% C	56,000	95,000	25	51			
As-rolled, at 0.50% C	60,000	101,000	22	43			
Normalized, at 0.40% C	58,000	94,000	27	53			
Normalized, at 0.50% C	62,000	100,000	23	46			
Annealed, at 0.40% C	52,000	86,000	28	55			
Annealed, at 0.50% C	54,000	92,000	26	54			

(Above data from Materials & Methods Manual No. 8, *Standard Engineering Steels* (September 1945))

steelmaking industries the need for extreme toughness may restrict the choice of base material to austenitic manganese steel. This welds easily but must be protected from excessive heat and thermal stresses. Since the heat of welding may cause structural transformations that reduce toughness it is well to be familiar with its behavior when welding.[5] Austenitic manganese steel has a low yield strength, but otherwise is excellent as a hard-facing base. Its resistance to crack propagation is an outstanding property.

With design that will cause the hard overlay to be stressed in compression, and that will provide the best practicable support for it, the hard-facing alloy may be selected primarily on the basis of the stresses expected. If they are below 150,000 psi. and the velocity of impact is below 80 ft. per second it is probable that a martensitic iron will give good service. If the expected stress or velocity exceeds these values, but the impact energy is probably below 400 ft.-lb. reasonable life with minimum deformation can be expected from martensitic steels. As impact energy exceeds 400 to 500 ft.-lb. it is usually advisable to employ manganese steel. Each of these materials will be discussed in some detail since other properties may affect the choice.

Abrasion is so frequently associated with impact that a wise selection cannot be made without considering it. Other properties, such as heat resistance, hot hardness, corrosion resistance may also be important.

For simplicity abrasion can be classified in three groups, typed as (*a*) erosion, or scratching abrasion, (*b*) grinding or high-stress abrasion and (*c*) gouging abrasion. Erosion is a low-stress phenomenon. Examples are plowshare wear in sandy soil or power shovel digging in sand (Fig. 15). Since impact is usually light or absent in such service, the high-chromium irons and the gas weld overlays from composite tungsten carbide rods[6] are suitable. As stresses rise, either from impact or abrasive conditions, the martensitic irons become appropriate.

Stresses on a microscopic scale increase at the wearing face if the abrasive is backed up, either by another metal face (examples are machine parts rubbing in a gritty environment or in actual grinding operations, as in ball mills) or by rock faces. Such conditions cause fracture of the abrasive grains or point by crushing,

Fig. 15 Power shovel bucket lip digging in sandy material. The abrasive conditions are here typed as erosion, and involve low unit stresses on a microscopic scale. Wear appears as polishing from minute scratching and impact stresses are usually negligible

but in the process the alloy face is subjected to concentrated stresses comparable to the crushing strength of the abrasive. For quartz this is probably in the vicinity of 30,000 psi. Martensitic irons are a good selection for such service.

Gouging abrasion implies high stresses on a gross scale, with associated impact. Power shovel digging in rock fragments (Fig. 16), crusher operation and the like are of this type. Prominent grooves are cut in the wearing surfaces, especially when these are soft. Martensitic steels are appropriate for these conditions unless greater toughness is required, in which case austenitic manganese steel is generally selected.

It is pertinent to note that the matrix of composite tungsten carbide arc weld deposits is frequently a martensitic steel.

The resistance of important hard-facing alloys to erosion as determined by laboratory tests employing a rubber wheel and dry quartz sand, appears in Fig. 17.[7]

Resistance to high stress or grinding abrasion is shown in Fig. 18, for the group of alloys suggested here as appropriate for impact service. Since the abrasion resistance follows a trend opposite to that of impact resistance (in the sense of toughness) the need

Fig. 16 Power shovel bucket digging in rocks and boulders. This wear situation provides gouging abrasion and considerable impact. Both are associated with high stresses and require tough alloys to withstand them. Austenitic manganese steel is usually the preferred material

for pertinent data and the exercise of engineering judgment is apparent.

For gouging abrasion the technique of using manganese steel, protected in spots by the more abrasion-resistant alloys, will be described later.

Table 7 provides a simplified classification of hard-facing alloys, together with notes about their salient properties. The first two, tungsten carbide and high chromium irons, are suitable for very light impact if gas welded. They are tougher but less abrasion resistant when arc welded. Neither is as appropriate for light impact, where high deformation resistance is desired, as the martensitic irons.

Table 7—A Graded Series of Wear-Resistant Alloys

Listed in order of decreasing abrasion resistance and increasing toughness.

←*Increasing toughness. Increasing abrasion resistance*→		*Properties sensitive to carbon content and structure*
1.	Tungsten carbide composites	Maximum abrasion resistance Worn surfaces become rough
2.	High-chromium irons	Excellent erosion resistance Oxidation resistance
3.	Martensitic irons	Excellent abrasion resistance High compressive strength
4.	Cobalt base alloys	Oxidation resistance Corrosion resistance Hot strength and creep resistance
5.	Nickel base alloys	Corrosion resistance May have oxidation and creep resistance
6.	Martensitic steels	Good combinations of abrasion and impact resistance Good compressive strength
7.	Pearlitic steels	Inexpensive. Fair abrasion and impact resistance
8.	Austenitic steels Stainless steels Manganese steel	Work hardening Corrosion resistance Maximum toughness with fair abrasion resistance. Good metal to metal wear resistance under impact

The cobalt base alloys should be selected primarily for hot hardness and oxidation resistance. They are needlessly expensive for impact and abrasion service

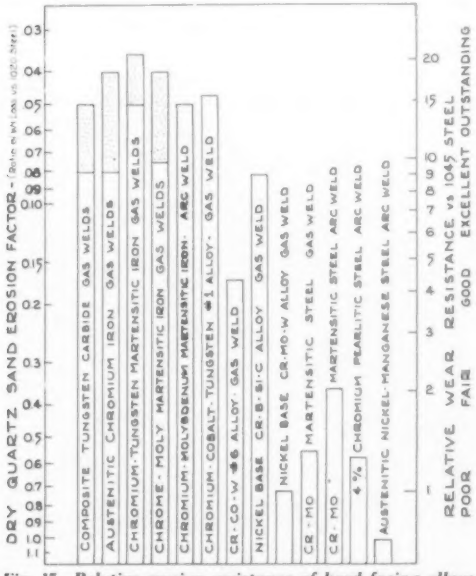

DRY QUARTZ SAND EROSION FACTOR
RELATIVE WEAR RESISTANCE vs 1045 STEEL
POOR FAIR GOOD EXCELLENT OUTSTANDING
COMPOSITE TUNGSTEN CARBIDE GAS WELDS
AUSTENITIC CHROMIUM IRON GAS WELDS
CHROMIUM-TUNGSTEN MARTENSITIC IRON GAS WELDS
CHROME - MOLY MARTENSITIC IRON GAS WELDS
CHROMIUM-MOLYBDENUM MARTENSITIC IRON ARC WELD
CHROMIUM-COBALT-TUNGSTEN #1 ALLOY GAS WELD
CR-CO-W #6 ALLOY GAS WELD
NICKEL BASE CR-B-SI-C ALLOY GAS WELD
NICKEL BASE CR-MO-W ALLOY GAS WELD
CR-MO MARTENSITIC STEEL GAS WELD
CR-MO " MARTENSITIC STEEL ARC WELD
4 % CHROMIUM PEARLITIC STEEL ARC WELD
AUSTENITIC NICKEL-MANGANESE STEEL ARC WELD

Fig. 17 Relative erosion resistance of hard-facing alloys against dry quartz sand

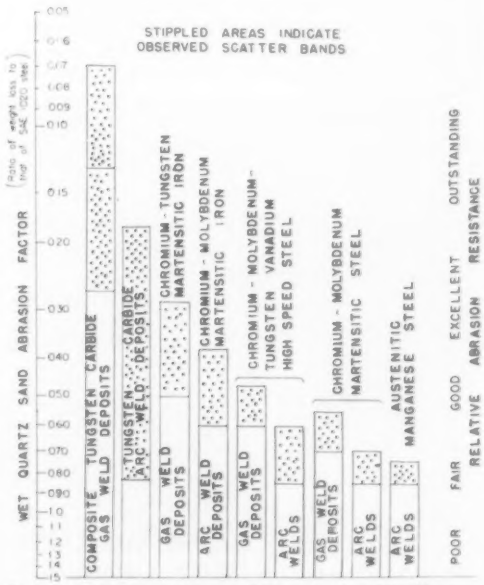

STIPPLED AREAS INDICATE OBSERVED SCATTER BANDS
WET QUARTZ SAND ABRASION FACTOR
COMPOSITE TUNGSTEN CARBIDE GAS WELD DEPOSITS
TUNGSTEN CARBIDE ARC WELD DEPOSITS
GAS WELD DEPOSITS
CHROMIUM - TUNGSTEN MARTENSITIC IRON
ARC WELD DEPOSITS
CHROMIUM - MOLYBDENUM MARTENSITIC IRON
GAS WELD DEPOSITS
CHROMIUM - MOLYBDENUM - TUNGSTEN VANADIUM HIGH SPEED STEEL
ARC WELDS
GAS WELD DEPOSITS
CHROMIUM - MOLYBDENUM MARTENSITIC STEEL
ARC WELDS
AUSTENITIC MANGANESE STEEL
ARC WELDS
POOR FAIR GOOD EXCELLENT OUTSTANDING
RELATIVE ABRASION RESISTANCE

Fig. 18 Abrasion resistance of some martensitic alloys

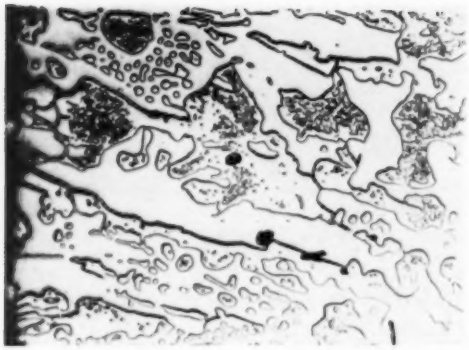


Fig. 19 White cast iron consisting of iron carbide, as massive areas and as small spheroids, and ferrite. This is the annealed structure of a freight car wheel tread

as well as less competent. Relative performance appears in Fig. 17.

The nickel base alloys are most appropriate for corrosion resistance. They may differ considerably in their properties, and are expensive. To simplify the selection problem they are not recommended here for impact service.

The pearlitic steels are generally softer but not necessarily tougher than the martensitic steels. They are more appropriate for build-up welding than for surfacing, though under some conditions they are applied for hard facing, particularly when a specific material is closely related in alloy content to one more typically martensitic. The structure of such steels is sensitive to weld cooling rates, and the types merge readily.

The austenitic stainless steels work harden, are nearly as tough and can frequently substitute for austenitic manganese steel. However, unless their corrosion resistance is required they are needlessly expensive for impact service.

Since most selections where impact is important will be made from the trio emphasized this far, each will now be described in more detail.

Fig. 20 Gray cast iron, consisting of graphite and pearlite. This is a normal high-strength engineering iron

Martensitic Irons

The alloys of this group are composed chiefly of iron and carbon, with incidental elements such as manganese, silicon and minor tramp elements, together with alloying elements that control the structural constituents which form during cooling after weld deposition. These control elements, such as chromium, nickel, tungsten, etc., must be carefully balanced to produce the desired structures.

Carbon contents are typically above 2%, thus differentiating them from the tougher but less abrasion-resistant steels. Ni-Hard[8–10] is an example of this type, and is composed nominally of

C, %	3.0
Mn, %	0.6
Si, %	1.0
Ni, %	4.5
Cr, %	1.5

Ordinary cast iron, in simple terms, is a mixture of steel (which usually is ferritic or pearlitic) and iron carbide (Fig. 19) or steel and graphite (Fig. 20). The former is brittle, hard (about 450 BHN) and has good resistance to some types of abrasion, though high-stress abrasion wears it rapidly. The latter is soft, machinable, and, while it does well as a metal to metal friction face, low in resistance to severe abrasion.

Martensitic irons are characterized by the steel portion of the structure in the form of martensite, or martensite associated with untransformed austenite. The matrix of the structure is carbide (Fig. 21). This carbide, which contains a portion of the alloying elements, is about 1200–1400 VPN.* The martensitic areas may be around 400–700 VPN. The average hardness of the composite structure as determined by conventional hardness tests, may range from 500 to 750 BHN or Rc 50–66.

* Vickers Pyramid numbers with a 25-gm. load are used here, since Brinell hardness is not adapted to such high levels. In the range below 600 VPN the values are roughly comparable to the corresponding Brinell hardness numbers.

Fig. 21 Martensitic iron gas weld deposit, consisting of alloyed iron carbide and austenite partially transformed into martensite

Structures and properties can be modified by welding methods and cooling rates. Gas welding with the recommended reducing flame, which has a feather 2–3 times as long as the inner cone, tends to add carbon to the deposit, increasing brittleness and perhaps hardness. Arc welding, in contrast, tends to burn out carbon and may produce tougher but less wear-resistant deposits.

Figure 22 shows a cross section through a chromium-molybdenum martensitic iron gas weld deposit on SAE 1020 steel. Marked diffusion of carbon from above into the ferritic base metal occurs, despite the short welding time. This carbon diffusion is exploited in facing by carburizing the steel surface with the torch until a thin layer of iron reaches the eutectic composition and melts at 2000–2100° F. The appearance of this layer, which glistens when it melts, is termed "sweating" and signals to the welder that the overlay can be readily flowed onto the heated surface.

Figure 23 shows an arc weld cross section of a similar material.

Tool steels form martensite when quenched from high temperatures. They have high strength and hardness, but wear resistance is not as high as for the very hard carbides. The martensitic irons combine both in one structure and thus have improved wear resistance along with greater toughness than the carbide would have alone. An added advantage of these irons is the property of forming martensite during the cooling from weld deposition, which greatly simplifies their application to wear problems by eliminating the need for a separate heat treatment.

The hard composite structure of the martensitic irons confers good resistance to abrasions of all kinds, which is primarily the reason for their selection. Moreover, of all the common hard-welding alloys, this group appears to have the highest compressive strength and thus promises the elasticity to resist light, direct, compressive impact blows without damage.

Resistance to erosion or scratching abrasion depends largely on the volume and hardness of the carbide constituent. Thus it is expected to be greater for gas in comparison with arc welds. The relative performance against this type of wear appears in Fig. 17. Depending on the type of alloy and the weld method, the weight loss in test may range from 10 to 30 times lower than soft steel. Note that tungsten carbide deposits are similar, but with a narrower scatter band. The test data here are believed to reflect chiefly the matrix character of the tungsten carbide deposits,[6] the hard granules being more wear resistant provided they are not lost by undermining. The matrix of tungsten carbide welds is frequently martensitic.

High chromium irons, because their carbides are harder, usually show better erosion resistance than the martensitic irons, but as stress on the abrasive rises, this pattern reverses. This has led to wide use of martensitic irons for general abrasion resistance. Their selection for light impact is much less common but it should be realized that both properties can be had in one alloy.

Figure 18 portrays relative resistance to high-stress grinding erosion. The graph has been simplified for convenience. If the relative performance of other alloys to this type of wear is needed for comparison, many of these can be found in previous publications.[1, 6, 11]

The elastic compression strength, and consequently resistance to light impact deformation, is very high for the martensitic irons. Ultimate compression strength up to around 400,000 psi. can be obtained by heat treating both irons and steels, but in weld deposits, where the desired structural development depends on

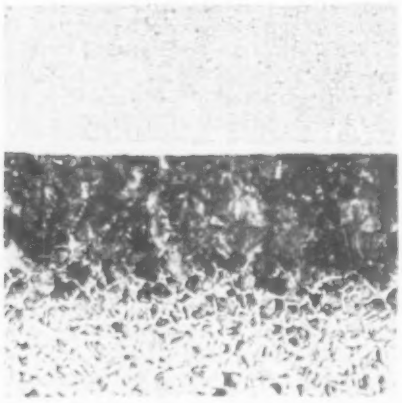


Fig. 22 Photomicrograph at 100 × of a gas weld deposit of Cr-Mo martensitic iron on 1020 steel. The overlay is a mixture of hard carbide (gray) and martensite (white). Below the weld junction a dark zone indicates that the steel has been carburized to a depth of about 0.02 in.

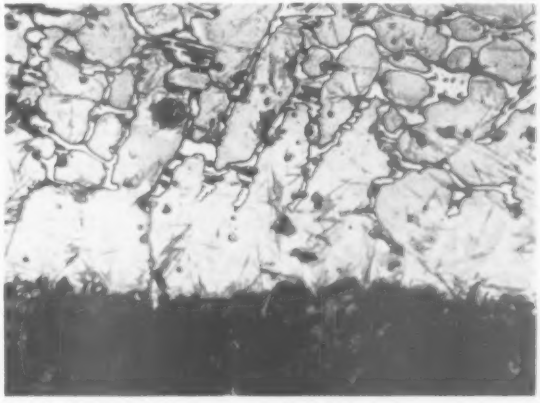


Fig. 23 Photomicrograph at 500 × of Cr-Mo martensitic iron arc weld on 1045 steel base. There is less carbide and more martensite, in comparison with a gas weld deposit, and abrasion resistance may be lower

transformation during air cooling, the irons seem superior. This is shown by the scatter bands of Fig. 10. The yield strength of the irons seems to be dependably at a higher level.

The influence of design where these irons are subjected to impact deserves great emphasis. Their strength in compression is much greater than in tension, so shear and tensile stresses should be avoided. They should receive competent support, with sufficient thickness in the deposit to spread the stress over an adequate area of the supporting material.

Martensitic Steels

In machine design the martensitic steels are frequently used and the designer is generally familiar with their properties, though he may know them as tool or structural steels. There are many alloy combinations used to obtain the hardening properties desired and formerly the influence of composition was considered to be complex. More recently there is much evidence to justify a simplified concept of alloying for strength based on hardenability. If full hardening is obtained the level is dependent largely on carbon content; the alloys contributing chiefly to the depth of hardening. Maximum hardness is attainable with about 0.55% carbon, though wear resistance may increase considerably at higher carbon levels.

Hardened martensitic steels can be softened and toughened by tempering. Alloys affect the rate and pattern of tempering, but again simplification is possible. At a given hardness level, obtained by full hardening and tempering as required, the mechanical properties of low- and medium-carbon martensitic steels may be estimated from Fig. 24.[14]

When these steels are used for hard facing the carbon levels may be higher than for structural use. This may confer greater brittleness, and for untreated weld deposits can cause considerable untransformed austenite to be retained. The austenite is softer than the martensite, bainite or pearlite that may appear in the structure and has a lower yield strength. This results in less resistance to impact deformation. Were it not for this pattern and the better abrasion resistance of the irons, these steels would probably be the preferred alloys for resisting impact with elastic strength. Practically, they fit well into the zone between the irons and austenitic steels.

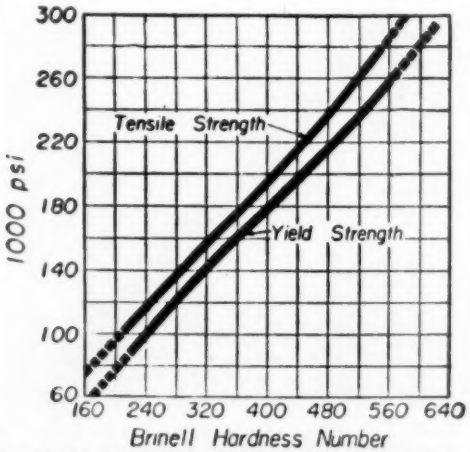


Fig. 24 Statistical mean of the relationships among Brinell hardness tensile strength and yield strength in quenched and tempered alloy steels (from A.S.M. "Metals Handbook" 1948 edition, p. 454)

It will be recognized that the alloy steels can provide a variety of structures: martensite, pearlites of differing fineness, bainite, austenite, undissolved carbide, and ferrite, depending on alloy balance, heating and cooling patterns and carbon content. Mixtures of these structures in one weld deposit are quite common. The suitability of a steel for hard facing thus depends largely on the metallurgical control in manufacture, the balancing of the alloys, the service intended and the sensitivity to welding conditions. Neither the designation "hard-facing alloy" nor the hardness of the deposit is a reliable criterion of merit.

A range of properties that would encompass all hard-facing steels that might be labeled martensitic would be so broad as to be almost useless. The ranges given herein have been narrowed to represent several well-controlled grades, chiefly of the chromium-molybdenum type.

The high-speed steels as a group are well adapted to hard facing. They contain such a large quota of alloying elements that hardening is assured almost without regard to the method of cooling. In weld deposits the structure will contain more undissolved carbide and more austenite than would heat-treated tools, but usually these do not seriously detract from performance of weld deposits. Since martensite will dominate the structure of gas weld deposits, they fit well into the present category. The structure of arc welds is quite variable and depends considerably upon the amount of dilution from the base. This dilution in turn depends on welding conditions. The effect of this variable on the abrasion resistance of weld deposits appears in Fig. 18. Note that the pattern is repeated for the other martensitic alloys.

Hot hardness data for martensitic steels have been published previously.[1]

Figure 18 applies to high stress abrasion, which with gouging abrasion is most appropriate to a discussion of impact, since they are more likely to be associated. Low stress or simple scratching abrasion is unlikely to involve much impact, and is more appropriately dealt with by high chromium irons or tungsten carbide hard facing. The erosion or low stress abrasion resistance of the martensitic steels is comparatively low (Fig. 17). Even if impact is involved, the martensitic irons are a much better choice provided they are not overstressed by it.

It is in the area where impact is severe enough to

overstress the receiving face into some plastic deformation that the martensitic steels perform best. They are stronger elastically than austenitic steels and thus have more flow resistance. The resulting better dimensional stability under impact, like that of the martensitic irons, is a marked advantage. In addition, unlike the irons if the yield strength is exceeded, they can tolerate considerable plastic deformation in compression (Fig. 10) before cracking occurs. A striking example of this appears in Fig. 13. The specimens of Cr-Mo martensitic steel endured 18% reduction in height under a cumulative impact of 50,000 ft.-lb. without cracking.

Despite this capacity for plastic flow, liberties cannot be taken with tensile stresses. High-carbon hardened steel characteristically has little tolerable tensile elongation and when used as a weld deposit this feature is somewhat accentuated. Cracks will quickly follow tensile flow. Thus the cracking tendency of hard steel deposits on a soft, uncompetent base is nearly as bad as that of the more brittle irons.

Austenitic Manganese Steel

Austenite, the tough, nonmagnetic, high-temperature structure of most steels, whose transformation provides the thermal hardening reactions, is retained at room temperature in some steels by virtue of their alloy content. The commonest examples are 18% Cr-8% Ni stainless steel and austenitic manganese steel. The latter usually contains 1.0–1.4% carbon and 10–14% manganese. It may be called Hadfield's steel, after its inventor. It is characterized by high strength, high ductility and excellent wear resistance. In the form of castings or of rolled shapes it serves many industrial requirements economically and has built up an enviable record as the outstanding material for resisting severe service that combines abrasion and heavy impact.

For many purposes Hadfield steel and the numerous austenitic stainless grades, can be interchanged, since they have some properties in common. The manganese steel is not corrosion resistant, however, while the stainless types are expensive. These two features usually control a choice between them. If temperatures above 500–600° F. are involved, manganese steel is considered unsuitable in most applications.

Austenitic manganese steel has been rather well standardized[12] and described.[5,13,14] Its conventional properties are detailed in Table 8, its vulnerability to heat in Fig. 25 and its capacity to work harden in Fig. 5. This work hardening capacity, which exceeds that of any other common steel, explains some of its wear-resisting properties and partially justifies its use for hard facing.

Manganese steel, which herein refers to that con-

Table 8—Properties of Hadfield's Austenitic Manganese Steel

Type	*Form*	*Composition, %* C	Mn	Si	Ni	*Yield strength, psi.*	*Tensile strength, psi.*	*Elongation, %*	*Red. area, %*	*Hardness, BHN*	*V-notch impact Izod Charpy*
Std.*	Cast 1 in. ϕ	1.0 1.4	10 14	0.2 1.0	*	50,000 57,000	100,000 145,000	30 65	30 40	185 210	100 ± 90 220
Std.*	Rolled 1 in. ϕ	1.1 1.4	11 14	0.2 0.6	*	50,000 60,000	131,000 142,000	40 60	35 50	170 200	90 150
Ni-Mn*	Cast 1 in. ϕ	0.84	12.5	0.48	3.46*	...	105,000 127,000	45 65	35 45	170 180	95 105
Ni-Mn*	Cast 1 in. ϕ	1.10 1.17	12.5 12.7	...	3.1* 3.6	...	113,000 140,000	44 70	35 45	150 170	105±
Ni-Mn	Rolled	1.1 1.4	10 14	0.2 0.6	3.25 3.75	50,000 63,000	130,000 155,000	48 72	40 60	165 195	90 150

Work hardening capacity: From 170–200 BHN to 450–550 BHN
Specific gravity: 7.9. Density 0.286 lb./cu. in. = 493 lb./cu. ft.
Melting point: 2450° F.
Thermal conductivity: 2.31 vs. copper at 100.‡
Electrical resistivity: 70 microhms/cm. cube or about 7 × that of pure iron.§
Electrical conductivity: 0.0143 megohms per cm. cube.§
Specific heat (thermal capacity): 0.12 g. cal./g./° C.
Thermal conductivity (0.8% C, 3% Ni)† 0.031 cal./cm./cm./sec./° C. = 90 Btu./sq. ft./in.° F.§
Magnetic permeability: 1.003–1.03 (H - 24).
Magnetic permeability: without surface preparation: 1.30±.
Wet quartz sand abrasion factor: 0.75–0.85 (vs. SAE 1020 as 1.00)[4]
Elastic modulus: 24 to 30 million psi.

Thermal expansion coefficients:
in. per in. per ° F.

	Av.
100–300° F.	0.0000102
100–500° F.	0.0000113
100–700° F.	0.0000119

Instantaneous	
100° F.	0.0000102
200° F.	0.0000106
300° F.	0.0000111
400° F.	0.0000119
500° F.	0.0000126
600° F.	0.0000134
700° F.	0.0000136

* Toughened by water quenching.
† Swanger, "ASTM-ASME Symposium on Effect of Temperature on Metals," 1931, p. 732.
‡ Pennington, H. R., "Engineering and Contracting," February 1922.
§ Varies with heat treatment.

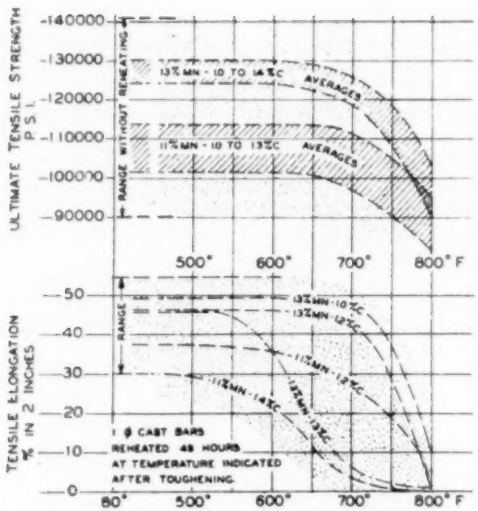


Fig. 25 Embrittlement from reheating austenitic manganese steel

taining about 13% Mn, is heat treated by water quenching in manufacture. In service, even for welding, it should be subjected to as little reheating as possible.

If standard manganese steel is employed as a welding rod, the weld cooling will induce brittle transformation products and the characteristic toughness will not be exhibited. This effect can be avoided considerably by lowering the carbon content. Therefore, as a welding alloy, the carbon range is usually reduced to 0.60–1.00%, contrasting with 1.0–1.4% for castings and wrought products, and sometimes is made even lower. Arc welding is expected to burn out some carbon, with the result that deposits may contain as low as 0.40%. Such alloys do not embrittle during the cooling that follows a weld as would the higher carbon type, but neither do they display the same toughness. To compensate, it is customary to add nickel, molybdenum or other elements to the electrode composition. The pattern of such changes is shown by Table 9.

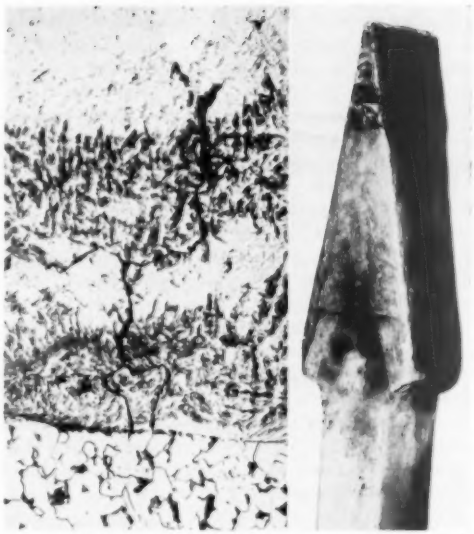


Fig. 26 Structures produced by arc welding nickel-manganese on carbon steel. 250 ×

Fig. 27 Austenitic manganese steel dipper tooth showing the scars of gouging abrasion. The blunted tooth has been retipped by welding on a manganese steel wedge bar with nickel-manganese steel electrodes. The tip has finally been protected in selected areas with beads of hard-facing alloy

Manganese steel for welding is intended to be applied on a manganese steel base. If a simple or a hardenable alloy steel is welded with manganese electrodes, the dilution will so change the alloy balance that brittle structures are likely to develop. Figure 26 illustrates what may occur. If for any reason it is necessary to apply manganese steel to hardenable steels it is advisable to separate them with a pad of austenitic stainless steel such as 26% Cr:20% Ni. This has enough reserve alloy content to withstand dilution without leading to brittle transformation products.

Gas welding is likely to add carbon to a deposit and increase the chances of brittleness. It will also tend to overheat the base metal and cause it to embrittle by

Table 9—The Contribution of Alloy Elements to Austenitic Manganese Steel Properties

Heat No.	*C, %*	*Mn, %*	*Si, %*	*Ni, %*	*Mo, %*	*Type of Material*
Av. expectancy	1.12	12.5	0.5	...	...	Standard Mn steel, cast
49-240	0.61	11.9	0.34	0.16	...	Low-carbon Mn Steel
49-304	0.73	11.5	0.37	0.20	...	Low-carbon Mn Steel
49-210	0.61	11.8	0.17	...	1.05	Molybdenum-Mn steel
50-546	0.75	13.9	0.58	...	0.94	Molybdenum-Mn steel
50-280d	0.72	12.6	0.54	3.41	...	Nickel-Mn steel

Material	*Yield strength, psi.*	*Ultimate tensile strength, psi.*	*Elongation in 2 in., %*	*Red. area, %*	*Hardness, BHN*	*Heat treatment, ° F.-Hr.-Q.*
Std. Mn steel	53,000	124,000	45	35	195	Water quenched
0.61% C Mn steel	42,000	96,750	21	18	167	1900-2-Water
0.73% C Mn steel	46,000	109,250	33	28	163	1900-2-Water
0.61% C Mo-Mn steel	47,000	110,500	35	26	175	1900-2-Water
0.75% C Mo-Mn steel	52,500	131,500	59	38	207	1900-2-Water
0.72% C Ni-Mn steel	44,500	121,500	58	36	187	1900-2-Water

transformation. The effect of 48 hr. at 800° F. (Fig. 25) may be achieved by perhaps 6 min. at 1100° F.

Because the time-temperature pattern is so important, welding should be carefully planned to minimize both factors. Obviously arc welding is preferable, and is employed almost exclusively.

The wear resistance of manganese steel has been somewhat obscured by misconceptions. Briefly it can be typed as poor for low stress or scratching, fair for high stress abrasion, good for gouging abrasion and outstanding for metal to metal wear with impact. Toughness, not abrasion resistance, is its outstanding quality. Its superiority where heavy impact is involved with abrasion has been so marked that there has been a tendency to miss the significance of the separate factors.

The assumption of excellent erosion or abrasion resistance when abrasive particle stresses are low has led to occasional disappointing results. These are then "explained" by the statement that the steel has had no opportunity to work harden, implying that this feature is essential.

If rock hardness is above that of unhardened manganese steel but below that after work hardening, it is reasonable to expect quite different performance. Limestone, dolomite and rocks of similar hardness could provide this pattern. However, the commonest mineral abrasive is quartz, usually encountered as an important crystalline constituent in granites, as the chief mineral in sandstone and quartzite, as an important accessory mineral in shales, clays and many other rocks, or as common sand and gravel. Quartz is harder than work-hardened manganese steel or hardened tool steel.

Using the reliable high-stress grinding abrasion test previously described,[6, 11] careful experiments have attempted to demonstrate how much work hardening affects the wear rate against quartz sand. The expected range of abrasion factors on heat treated but unwork-hardened manganese, at 200 Brinell or below (about Rockwell C20) is 0.80 ± 0.06, representing a scatter band ± 2 standard deviations about the mean.

On the material selected for experiment the abrasion factor was 0.77 for 1.14% carbon, 12.4% manganese steel and 0.78 for 1.10% C and 12.6% Mn with 2.0% chromium. After work hardening to near Rockwell C50 by heavy repeated impact, a series of abrasion tests gave factors of 0.74 to 0.85 for one specimen of the first and 0.80 to 0.84 for the second. Several additional test series did not disprove this pattern though they did expand the scatter band to bracket factors from 0.69 to 0.93 for short runs to test thin layers. The greater spread is attributed to the larger experimental error of 5-min. tests as compared to the standard 1-hr. run.

The work-hardened zone was not worn through by this testing since the impact treatment was designed to harden in depth. The worn-away material was at least Rockwell C42, from a cross sectional hardness survey, while the abraded face exhibited values from Rc47 to Rc55. From these data it may be concluded that work hardening and resistance to high stress abrasion by quartz are not significantly related.

Gouging abrasion, which is usually coupled in the field with severe impact, tends to cut major grooves or deep scratches in the wearing face. The digging shown in Fig. 16 is a sample of the service causing gouging abrasion. The wear pattern may be seen in Fig. 27. This is a worn manganese steel dipper tooth that has been repointed by welding and then hard faced at critical areas.

This gouging seems to be related to machining and it is probably significant that manganese steel is very difficult to machine. The hard irons, other brittle alloys, and fully hardened steels are usually more difficult, and they are more abrasion resistant, but substitution of them in service conditions such as shown in Fig. 16 or in jaw crushers and the like, generally leads to premature fracture. Here the great toughness of manganese steel is a necessary asset.

It is difficult to disentangle the cutting or gouging wear action from the impact in this area, and correlating laboratory tests have not been satisfactorily developed. The lack has not handicapped selection, however, since manganese steel has no recognized superior in this field—and moreover it is not expensive.

Metal to metal wear with impact, as typified by railway trackwork, is another area where manganese has no present equal. Work hardening is obviously an asset. The greatest drawback is the low initial yield strength, which permits considerable deformation before the elastic strength is raised by impact deformation.

Occasionally fairly tough, hardened steels are substituted for manganese steels subjected to impact in the railway, mining, quarrying and construction industries. At first the hardened steels seem to hold their own on a cost and service life basis, but after several seasons there develops a tendency to return to the austenitic alloy. Cold weather is one reason, since low temperature sharply increases the impact brittleness of the martensitic steels, while manganese steel is little affected down to −50° F. Perhaps more important, however, is a distinctive property of Hadfield's steel: its marked resistance to crack propagation. Even fatigue cracks which would cause prompt failure in most steels grow so slowly that they can be watched for months in service before replacement is necessary. This is evident particularly in relation to trackwork inspection. Perhaps this behavior is conferred by work hardening at the root of the crack. Similar cracks in other steels could not be tolerated and would be cause for immediate replacement.

Notched bar impact tests suggest the same pattern of toughness. With machine ground notches a Charpy value of around 100 ft.-lb. is representative. If the notches are tooled with a cutter, however, and the notch is thereby work hardened somewhat, the Charpy bar can sometimes take 220 ft.-lb. in the blow, be bent,

and dragged through the machine without complete fracture.

Since arc-weld deposits made under apparently good conditions will sometimes contain concealed cracks, this resistance to crack growth is a definite asset in the overlay as well as in the base.

The compression and impact behavior of manganese steels is shown in Figs. 5, 10 and 13. It is noteworthy that the eventual resistance to flow in this test exceeds that of the harder steels, though considerable plastic flow is necessary to reach this point. The continual strengthening that acompanies flow also operates in tension. A test with a conventional ultimate strength of 128,000 psi., when calculated on the actual final specimen diameter, which is very uniform with little necking, may be around 210,000 psi. Ultimate compression strength, if calculated on the same basis, is similar.

Because manganese steel is in some respects quite different from other steels it merits more discussion of welding precautions.

If manganese steel is deposited on carbon or low-alloy steel the transition zone may be too low in manganese and develop a martensitic structure, which because of brittleness can permit spalling of the weld deposit. Such use of an austenitic manganese steel overlay for wear resistance is generally not recommended, an air-hardening steel or cast iron being usually more satisfactory. Hard surfacing of manganese steel with a more abrasion-resistant alloy is practicable, however, and is indicated where the toughness of manganese steel is required only intermittently. Such composite structures should follow the principle of having the base as the tougher of the two.

Arc welding procedure should be aimed at:

(*a*) Welding only on sound, clean, unwork-hardened base metal that has been properly heat treated.
(*b*) Minimum heating of the base metal.
(*c*) Minimum mixing of deposit and base metal.
(*d*) Minimum thermal stresses in the weld deposit.

1. It should be remembered that castings, the most common form in which manganese steel is used, may contain cavities, spongy areas of shrinkage porosity, or entrapped sand. One of these may have been responsible for a fracture that requires welding repair. If so, it should be eliminated before proceeding, and in any case precautions should be taken to avoid such defects, as any one of them may prevent obtaining a successful weld. Work-hardened manganese steel embrittles more rapidly from reheating than the unchanged austenite; therefore, hardened metal should be removed by grinding. In the field a center punch mark provides a convenient test, the grinding being continued until a deep indentation is readily produced.

2. Minimum base metal heating, which is required because of the tendency for the heat-affected zone to embrittle (Fig. 28), is promoted by brief welding, a short arc, low current, and small diameter rods.

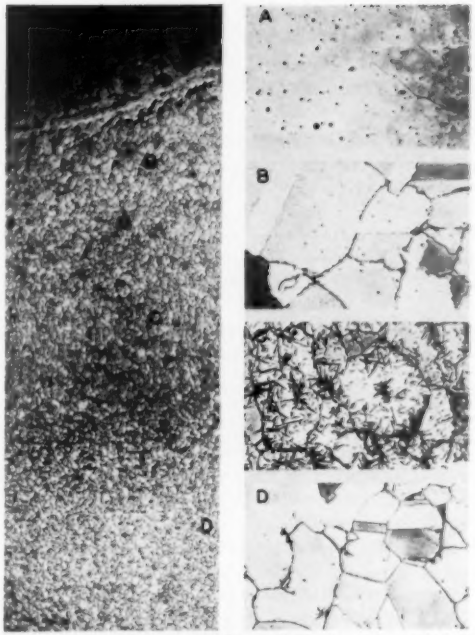


Fig. 28 — Arc-weld zone and heat-affected structures below a 4.0-kw., 27-v. weld on wrought austenitic manganese steel plate using $^3/_{16}$-in. bare nickel-manganese electrodes. Magnification at 10 ×, with detail photomicrographs at 125 ×

3. Minimum mixing of metal prevents adding more carbon to the weld deposit than is desirable for toughness. Also, excessive mixing is indicative of too much heat with its embrittling tendencies.

4. Thermal stresses develop as the weld deposit contracts during cooling. With expansion coefficients about 30% greater than usual structural steels, and a low thermal conductivity that tends to localize heating very sharply, the austenitic steels characteristically develop high local stresses. While manganese steel has high ductility when strained in one direction, the two- and three-dimensional stresses that occur in weld deposits can and frequently do cause cracking. The stress system is held responsible, and one remedy lies in modification of the stresses. These are tensile in a weld bead. They may be changed to compressive stress by pounding the deposit. Such peening, preferably with a pneumatic hammer, flows the outer surface and the deformation relieves the tension that would otherwise cause cracks. The pounding, for which a machinist's ball peen hammer is suitable, should be done promptly after deposition of a half rod, while the deposit is red hot. In no case should a bead longer than 2 in. be left without immediate peening.

Both bare and coated electrodes are used for welding, the bare rod frequently being preferred because of its higher deposition rate.

The lowest practical welding current is expected to

result in least damage to the parent metal. The work should be negative and the electrode positive (reversed polarity technique) for d.-c. welding. It is advisable to determine the minimum current for satisfactory arc behavior and penetration on a separate piece of metal before proceeding with an important weld.

Some structural changes that can occur from welding on manganese steel are illustrated in Fig. 28. This weld was on rolled manganese steel plate, which appeared to be more sensitive to reheating than cast material, perhaps because of a higher carbon content. Note that there is little grain boundary material to impair toughness in the weld metal itself. In contrast, the base has suffered considerably from carbide precipitation. The immediate under bead zone has been reaustenitized by welding heat, thus avoiding the acicular pattern noted further down, but the slow cooling after welding has permitted considerable grain boundary carbide precipitation. The same effect is noted even further below the weld, but without reaustenitizing. Between these two zones precipitation has been more severe, as evidenced by the acicular pattern, which is associated with very low tensile ductility.

To avoid these unfavorable structures, manganese plate that is to be welded is usually made to a low-carbon specification containing nickel like the nickel-manganese electrodes. Note that thermal stresses have torn the underbead zone in the base metal of Fig. 28. Even with cast manganese steel, which is frequently less vulnerable to these effects, it is not always possible to avoid bead and underbead cracking unless the welding conditions are favorable and peening is used to minimize tensile stresses before they can damage the weld area.

A recent series of experiments provides valuable evidence on the relation of welding technique to bead cracking. It seems likely that these data may be applicable to austenitic stainless steels also, which, like manganese steel, are subject to bead cracking or fissuring.

To minimize or eliminate personal equations in welding, a program was conducted with automatic equipment. An Auto-arc welding head fitted with a stick-feeder attachment provided a means of feeding the bare nickel-manganese electrodes under close voltage and arc power control. The work was moved, rather than the electrode, by a traversing table whose speed regulated the size of the bead deposited. Straight stringer beads (a severe condition) were made on base blocks of manganese steel from a single heat, using dummy run-off blocks to remove crater cracks from the test bead area. The machine operator, by watching recording meters, was able to hold arc power within narrow limits for each test weld.

Specimens in duplicate were made at three levels of arc power, with three bead sizes at each level. The beads were then split longitudinally by an abrasive cut-off wheel, hot etched to reveal cracks, examined at low magnification, and the number of cracks in the 2-in. test length counted for each specimen. The results were treated statistically,[15] providing the analysis of variance in Table 10. Photographs of some of the welds appear in Figs. 29, 30 and 31.

Table 10—Analysis of Variance—Weld Cracking

Variation source	*D. F.*	*Variance (mean square)*	*%*	*P*
Power	2	48.22	53	0.01/0.001
Bead size	2	29.54	33	0.05/0.01
Interaction	4	8.40	9	0.2/0.1
Error	9	4.28	5	
Total	17	90.44	100	

Revised: 58 + 35 + 7 = 100

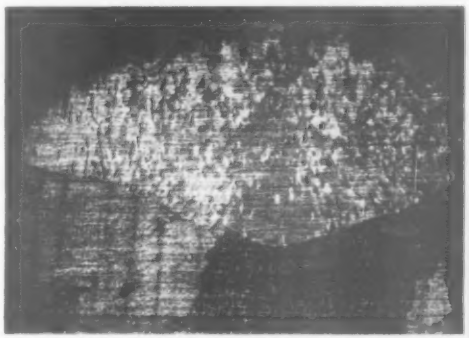


Fig. 29 Etched cross section of arc weld on standard austenitic manganese steel, using bare nickel-manganese electrode. Arc power was 4.0 kw; burn-off rate about 13 in. per minute (Fig. 32). The bead cross sectional area was about 0.19 sq. in. with a width of about 1.0 in. These conditions produced crack-free beads even though peening was not used. Had it been used, avoidance of the base metal cracks seen here would also be expected. 5×

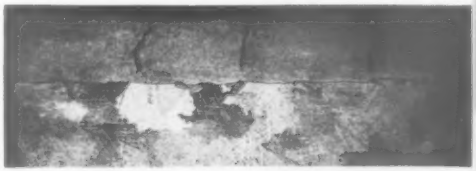


Fig. 30 Etched longitudinal section through 7.0-kw. arc weld with a medium (0.17 sq. in.) bead size, as a part of the study shown by Fig. 32. These cracks typically occur if beads are too small and arc power and burn-off rates are too high. Peening is expected to partially offset such unfavorable factors

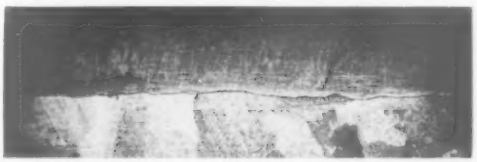


Fig. 31 Etched longitudinal section through 7.15-kw. arc weld similar to Fig. 30 but with a larger bead (0.22 sq. in.) which has made cracking somewhat less prominent

Figure 29 is a cross section of a low-power, large-bead weld. The bead itself is crack free but the base underneath shows cracks, one of which is beginning to penetrate the weld. Peening is desirable to avoid these underbead cracks.

Figure 30 is a longitudinal section of a high power, medium bead weld. Here three distinct cracks appear in the bead, two of which would have been visible under close surface inspection.

Figure 31 is a similar longitudinal section of a high-power, large-bead weld. Although seven cracks appeared under low magnification (and were used for the statistical calculations) only one is comparable in size to those of Fig. 30, attesting to the desirability of spreading thermal stresses over a large area.

This factorial experiment led to the conclusion that arc power was responsible for 58% bead size 35% and experimental error 7% of the observed variation, with a probability of less than one in twenty that the results could have been due to chance. The data are shown graphically in Fig. 32 which also implies the inadvisability of exploiting burn-off rates above 12 in. per minute if bead cracking is to be avoided. Of course, visual weld inspection is less severe than this test procedure. Also, cracks are sometimes not particularly detrimental. If they are undesirable in a critical application, Fig. 32 and the precautions outlined previously are worthy of careful consideration.

The factor of arc power can operate to accentuate thermal gradients and to increase the proportion of base metal that mingles in the fused bead. Thermal gradients increase stresses, while the base metal carries additional carbon into the bead. Both of these trends should be minimized as much as possible. The bead size is believed to operate chiefly by lowering unit stresses because the thermal stress is spread over a larger area. This explains partially why a weaving bead is preferred in manganese steel welding.

Since welding machines tend to provide nearly constant current to the arc, the control of arc voltage is a means of controlling power without changing the machine adjustment. Lengthening the arc will increase its voltage. The welder should aim at holding the arc as short as possible to minimize cracking.

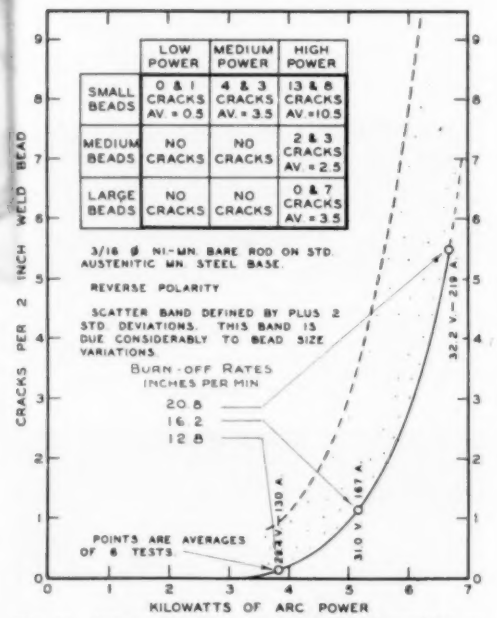

LOW POWER
MEDIUM POWER
HIGH POWER
SMALL BEADS
0 & 1 CRACKS AV. = 0.5
4 & 3 CRACKS AV. = 3.5
13 & 8 CRACKS AV. = 10.5
MEDIUM BEADS
NO CRACKS
NO CRACKS
2 & 3 CRACKS AV. = 2.5
LARGE BEADS
NO CRACKS
NO CRACKS
0 & 7 CRACKS AV. = 3.5
3/16 Ø NI-MN. BARE ROD ON STD. AUSTENITIC MN. STEEL BASE.
REVERSE POLARITY
SCATTER BAND DEFINED BY PLUS 2 STD. DEVIATIONS. THIS BAND IS DUE CONSIDERABLY TO BEAD SIZE VARIATIONS.
BURN-OFF RATES INCHES PER MIN
20.8
16.2
12.8
POINTS ARE AVERAGES OF 6 TESTS
22.2 V — 130 A
31.0 V — 167 A.
32.2 V — 219 A.
CRACKS PER 2 INCH WELD BEAD
KILOWATTS OF ARC POWER

Fig. 32 Apparent dependence of weld bead cracking on arc power and bead size

Applications

To list the many field conditions where hard facing can minimize impact wear is impractical here. Instead, several examples that illustrate principles will be given, from which ingenuity can derive many others by using the data on materials provided by this paper.

Light impact occurs typically in machine parts, such as valves, cams, tappets, rocker arms and the like. As a rule martensitic irons serve best for these. An example[11] in Fig. 33 is a PT boat rocker arm, which had been giving frequent trouble from wear. After being hard faced with a martensitic iron, it outlasted the motor, appearing in good shape when the engine was torn down after wearing out.

Hammer mills are locations of medium impact. Of course, the individual mills may differ in their stress levels. Generally a martensitic steel is a good first choice for overlays, though manganese steel may be the standard material for the hammers. Sometimes the impact conditions are not too severe for the martensitic irons in which case they will out perform the steels under severe abrasion. This is worth finding out for a given mill, since hammers are frequently

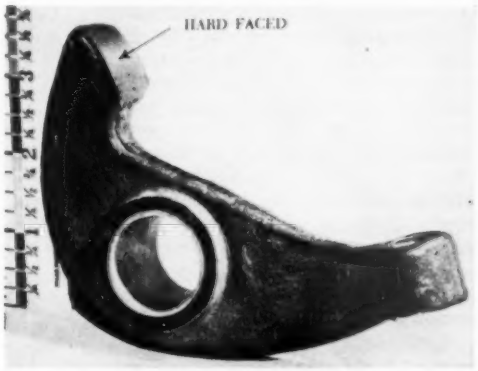


Fig. 33 PT boat rocker arm, hard faced at the critical wearing point with a martensitic cast iron. The procedure changed the part from a problem to an item that outwore the motor

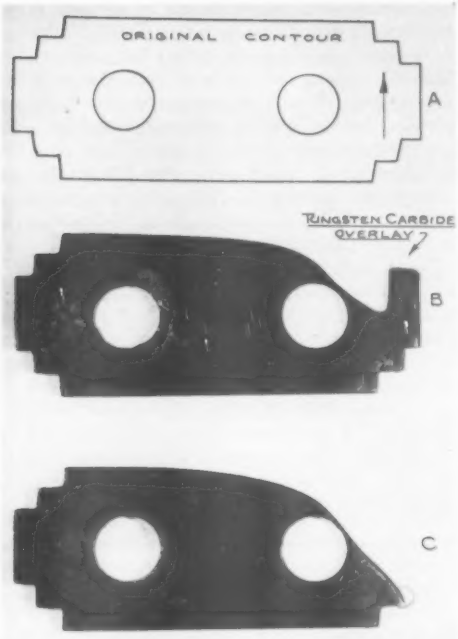


Fig. 34 Pulverizer hammers, showing (A) original contour, (B) wear pattern when tip is protected with a welded overlay of tungsten carbide and (C) wear pattern of unprotected hammer. The hard-facing procedure increased hammer life by 600% in service pulverizing asphalt roofing trimmings. The arrow indicates direction of motion. This is a reversible hammer, which explains the two holes

Fig. 36 Location of protective hard-surfacing weld beads on the lip of a walking drag line bucket operating in the Minnesota ore range. The bucket itself is of austenitic manganese steel

replaced and hard facing is comparatively easy. In cases of extreme abrasion and light impact, it has been worth while to use tungsten carbide to protect a critical area, as in Fig. 34.

Many heavy impact applications are in the mining, quarrying and construction industries. Jaw crushers are typical; the impact stresses are so great that hard alloys usually fracture before they wear out. Manganese steel is preferred from experience, and wear is frequently compensated by replacing the metal with a nickel-manganese weld deposit. To save effort, the overlay may incorporate rounds, flats or other shapes along with the weld metal. These wrought shapes, of austenitic manganese steel, are available along with the electrodes from some manufacturers.

On open-pit mining equipment, like the heavy buckets of Fig. 35, the wearing edges are protected with manganese steel weld deposits, sometimes over-

Fig. 35 Heavy power shovel bucket lips being protected with wear-resistant overlays

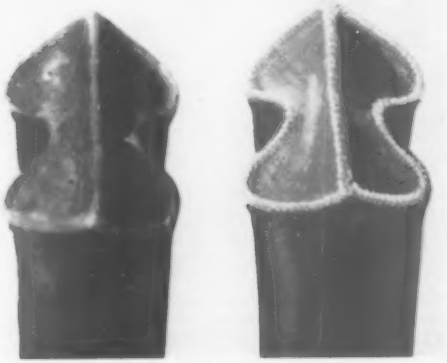


Fig. 37 Churn drill bit hard facing. This procedure has been successful in softer rocks, such as limestone

laid with harder alloys (Fig. 36). In this case the top protective beads are a martensitic iron.

Churn drill bits provide a service condition that invites hard facing, but the technique is not uniformly successful. Figure 37 illustrates drill bit points from a quarry location where holes for blasting charges are drilled in limestone containing some flint. Surfacing the critical edges with a martensitic iron increased the drilling life between regrinds from 30 to 60 hr., with additional savings because of fewer bit changes.

As in many field situations, some knowledge and consideration of the mineral or rock is necessary. If this favorable result in drilling limestone is accepted as applicable to all rocks, there are likely to be many disappointments, since rock hardness and toughness vary between wide ranges.

Bureau of Mines experiments[16] in hard-facing cruciform bits for percussion pneumatic drills (below 3 in. in diameter) found that composite tungsten carbide weld deposits provided marked advantages over ordinary heat-treated steel in drilling oil shale.

Using standard Timken H. Type detachable cross bits, the results of thousands of feet of drilling with Gardner-Denver CF79, column-mounted, 3-in., continous feed, drifter drills show that a $2^1/_8$-in. diameter hole can be drilled at an average rate of 19 in. per minute with 84 psi. air pressure at the drill. The price of the bit was \$0.25; each bit is resharpened twice at \$0.10 per sharpening, and used three times. About 5 ft. of hole was drilled between sharpening at a cost of \$0.45 per 15 ft. or \$0.03 per foot of hole.

When the bits are hard surfaced by applying composite tungsten-carbide oxyacetylene weld deposits from a $^5/_{32}$-in. diameter mild steel tube rod containing 50% carbide granules below 30 mesh, and subsequently heat treated by hardening (1450° F., $^1/_2$ hr., W. Q.) and tempering (300° F., 1 hr.), they can be depended upon to drill 100 ft. of hole without failing or losing gage. The \$0.25 bits cost \$0.50 to hard face, providing 100 ft. for \$0.75 or $^3/_4$ cents per foot. The hard-faced bits may drill up to 200 ft. with some loss of drilling speed. Hard facing thus reduces the cost of bits by 4:1, but the principal saving is in the cost of drilling labor (changing drill rods, shifting position of the drill and changing bits).

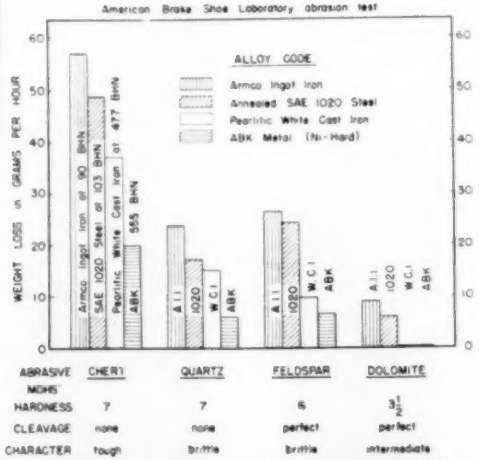


Fig. 38 Relative abrasiveness of common mineral abrasives, using as a criterion metal weight loss in the American Brake Shoe Laboratory abrasion test

The oil shale is tough but relatively soft. The term is applied to a group of marlstones, chemically similar to limestones, containing organic material that has a considerable influence on properties, compressive strength varying inversely though impact strength increases with the organic content.

It is probably significant that the successful examples of hard-facing rock bits have involved drilling in limestone rocks. The abrasiveness of dolomite (a common high magnesia limestone) in comparison with quartz, feldspar and flint is shown in Fig. 38. In Table 11 several important rock properties are summarized. Note that the limestone types are relatively weak in compression and have lower impact toughness and abrasive hardness than the hard massive rocks like

Table 11

Rock Type and Locality	*Compressive strength, psi.*	*Impact toughness, in./sq. in.*	*Abrasive hardness,* $Hd = \frac{10^{-3}}{Md}$
Marble, dolomitic (from Cockeysville, Md.)	32,000	2.6	7.6
Limestone, fossiliferous (from Bedford, Ind.)	10,000	2.1	2.6
Granite, massive (from Barre, Vt.)	34,000	5.0	19.0
Sandstone, porous (from Amherst, Ohio)	9,000	1.3	1.5
Sandstone, weakly cemented (from Waterfored, Ohio)	5,500	1.4	1.2
Slate, fine-grained (from Bangor, Pa.)	27,000	3.7	3.3
Greenstone, $^1/_2$–$^1/_2$ epidote (from Mount Weather, Va.)	39,000	6.5	20.0
Quartzite, strongly cemented (from Ishpeming, Mich.)	43,000	12	29
Iron ore, hematite (from Soudan, Minn.)	88,000	35	24
Jasper (from Soudan, Minn.)	99,000	26	20
Oil shale, limestone (from Rifle, Colo.)	17,000	3.7	10

(Values given are chiefly rounded off averages, and do not reflect rather wide scatter bands that may be encountered. Data abstracted from U. S. Bureau of Mines Report of Investigation R. I. 3891, August 1946.)

Compression tests made on $^7/_8$- to $2^1/_8$-in. diameter core drill samples with length about equal to diameter, air dried for 2 weeks. (A.S.T.M. designation C170–41T, 1942.) Impact toughness is from a drop hammer test, with incremental blows from a 2-kg. hammer on $^7/_8$- to $1^5/_8$-in. diameter (with length-diameter) specimens. (A.S.T.M. designation D-3-18, 1942.) Toughness reported is height required to produce fracture divided by cross-sectional area. Abrasive hardness is determined by grinding abrasion on an 18-in. diameter steel top at 22 rpm. with 40-mesh silicon carbide, and 8.53 psi. on the $^7/_8$- to $1^5/_8$-in. diameter rock faces. The hardness is expressed as the reciprocal of 1000 times the weight loss of the specimen (Md) in pounds per square inch per revolution.

granite, quartzite, hematite and chalcedony (of which flint, chert and jasper are varieties). The hard facing of churn drills with an air-hardening martensitic steel, working in some of the latter group of hard and strong rocks, has proved successful in some of the cases attempted, the alloy steel having enough toughness under the impact load to prevent spalling or chipping and also having enough hardness to maintain the cutting edges. However, it is not expected that all service problems can be met by using only the experience available, since this is a fertile field for experiment.

The magnitude of impact in churn drilling is high, as is indicated by the following calculations:

The steel shank may range from 18 to 28 ft. long	1740 to 2700 lb.
The steel bit may be 30 to 36 in. long	290 to 290 lb.
The shank may be 6 in. in diameter while the bit may flare to cut a 9-in. diameter hole	
Total weight may be	2030 to 2990 lb.
With a 2-ft. drop at each stroke	×2 ×2
Impact energy in foot-pounds	4060 to 5980

With impact of this magnitude there is always the possibility that the base metal will deform from the stresses transmitted through the hard facing and thereby crack the overlay in tension. Heat treating the entire point, as was done in the Bureau of Mines experiments, is helpful in avoiding underbead flow.

Railway trackwork provides a very common example where manganese steel is the standard material. While most rails are of pearlitic carbon steel, the intersections where wheel loads and impact provide double punishment are nearly all made of manganese steel castings. These frogs, crossings and the like batter down slowly from the very high impacts reported as reaching 33,000 ft.-lb. per wheel.[3] When too much flow makes the crossing bumpy, the work-hardened material is ground out and replaced with weld metal. Since safety is a primary responsibility, this application represents the most exacting use of manganese steel electrodes. Welders doing this work are usually skilled at avoiding the difficulties that may plague ignorant or careless use of this material.

Composite Structures

There are marked advantages that can be obtained by careful design of wearing parts. Combined with hard facing, not only can life be increased, but functional qualities are also benefitted. A good example is the self-sharpening dipper tooth illustrated in Figs. 39 to 41. Figure 39 is a side view of two teeth, the one on the right being standard manganese steel. The left tooth has seen more service, but was selectively protected by hard facing.

Field reports on this composite tooth pointer tend to indicate a doubling of service life in comparison with teeth repointed with wedge bars and "hard surfaced" in the usual manner. Some ratios have been as high as 4:1. If the comparison is made with teeth that are not hard faced at all the life expectancy

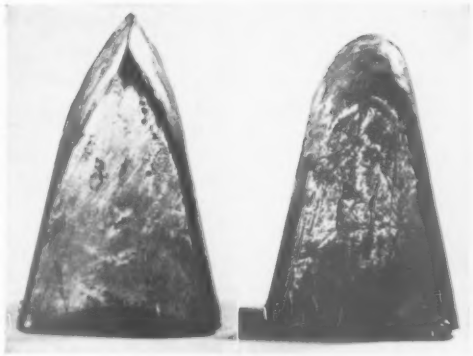


Fig. 39 Worn power shovel dipper teeth. The left example retains a sharp cutting edge as a result of hard facing in a careful pattern, even though nearly half its length has worn away. The rounded, inefficient, blunt contour of the right-hand example is typical of ordinary unprotected teeth

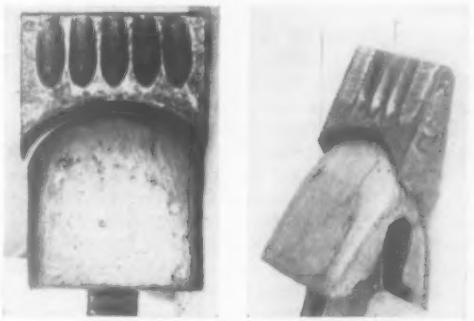


Fig. 40 The favorable wear pattern in Fig. 39 can be obtained by using cast manganese steel repointers with the groove design illustrated here. The outer and end grooves are filled with hard facing alloy as in the right hand photograph. The crescent backed repointer is then welded to the curved end of the worn tooth, providing an effective replacement that is less expensive than the original, but which may outwear it several times while maintaining an efficient cutting edge

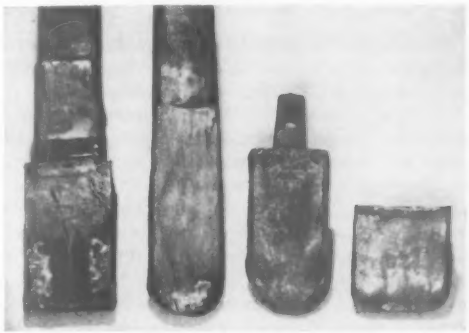


Fig. 41 The left view is of the assembly that the corrugated hard-face shoe makes possible. The face wear pattern (right-hand example) is as superior as the edge wear pattern shown in Fig. 39. These contrast with the blunt, rounded points produced by wear on the two unprotected teeth in the center. Note the heavy scratch grooves caused by the gouging abrasion to which these were subjected

Table 12—Hard-Facing Alloy Selection for Impact Service

Material → / *Suitable for →*	*Martensitic irons** *Light impact*	*Martensitic steels** *Medium impact*	*Austenitic manganese steel* *Heavy impact*
Yield strength tensile, psi.	60,000–80,000	60,000–80,000	45,000–55,000
Ultimate tensile strength, psi.	65,000–90,000	60,000–125,000	100,000–150,000
Tensile elongation, %	0.01–0.04	0.01–0.10	30–70
Brinell hardness	500–750	500–750	160–220
Rockwell hardness	C48-C65	C48-C65	C0-C20
Yield strength compressive 0.10% set	150,000–285,000	76,000–175,000	45,000–55,000
Ultimate compressive strength, psi.	265,000–385,000	180,000–385,000	200,000–310,000
Elastic deformation, %	0.8–1.4	0.8–1.5	
Plastic deformation, %	0.2–5.4	1.0–22	30–55
Total deformation, %	1.2–6.5	1.8–23	
Wet sand abrasion factor†	0.30–0.50	0.65–0.80	0.75–0.85
Dry sand erosion factor†	0.04–0.10	0.15–0.55	0.41–1.00

* Untempered, corresponding to the usual status of weld deposits.
† Weight loss ratios compared with annealed SAE 1020 steel.

ratio is perhaps doubled again. Besides the better life, the protected tooth has much better digging efficiency. The sharp edge is the result of controlled wear, even though the tooth is 30% shorter than the original.

The procedure for obtaining this pattern can be used for either a recessed new tooth, a repointer bar or a shoe that will fit over a worn tooth. The photographs show the repointer type of repair for a worn tooth. Figure 40 includes two of these, curved to fit the worn stub and recessed outside for the hard-facing deposits. The right-hand sample shows the hard facing in place. Note that the middle recesses are not filled. If these are hard faced also, the tooth will wear to an oval contour, as in the center examples of Fig. 41. When faced as in Fig. 40, the corners stand up much better, as in the right-hand tip of Fig. 41.

After hard facing the recessed tip or shoe, it is welded into place on the tooth shank. If repointer bars are used as in Fig. 27, they are welded directly to the shank. Austenitic alloys should be used for this assembly welding since the shoes or repointers are of manganese steel.

SUMMARY

This paper offers a suggestive rather than a rigorous analysis of some factors that will aid in using hard-facing alloys to avoid or minimize impact wear.

1. The preferred alloys for light, medium and heavy impact are suggested as martensitic irons, martensitic steels and austenitic manganese steels. Their properties are briefly summarized in Table 12.

2. The limit of light impact is suggested as the energy that does not create more than 150,000 psi. maximum stress in the surface struck. Impact velocity should also not exceed 85 ft. per second.

3. Medium impact is assigned that range where energy is likely to exceed by moderate amounts the level that can be absorbed by fully elastic behavior of strong materials. Some flow will therefore occur, but good life will be obtained from alloys that combine strength with considerable plasticity in compression.

4. Heavy impact causes flow of the strongest materials to such an extent that toughness rather than elastic strength is the primary requirement.

5. Formulas for calculating impact and impact stresses are included here for convenience, but must be used with critical caution. The actual deformation caused in a structure by impact may be very difficult to determine.

6. Tensile stresses should be avoided in designing hard faced areas for impact.

7. The base under a hard facing should be strong enough to avoid plastic flow under the overlay. Adequate thickness of deposit is helpful here.

8. Velocity can cause surface deformation independently of the energy of an impact blow. Calculation of this may be simplified by the nomograph of Fig. 6.

9. Photoelastic patterns indicate that stresses decrease rapidly below a surface receiving a concentrated load. Reasonably thick, strong overlays therefore can reduce unit stresses to levels that will not damage weaker base metals.

10. Edge impact on brittle hard-facing alloys should be avoided if possible; if not, the edges should be chamfered.

11. Compressive yield strength and tolerable deformation are believed to be related to impact resistance. Preliminary values of these for important hard-facing alloys are graphed in Fig. 10.

12. Repeated impact testing provides a means to rank hard-facing alloys (Fig. 13).

13. The selection of hard-facing alloys for impact will involve consideration of abrasive conditions. Low-stress and high-stress abrasion versus various alloys are graphed in Figs. 17 and 18. For gouging abrasion austenitic manganese steel is recommended.

14. Martensitic irons usually provide the best combination of abrasion resistance and elastic strength to take light impact without damage.

15. Martensitic steels provide better toughness but less abrasion resistance and elastic strength, as weld overlays. The strength can be improved by heat treatment.

16. Austenitic manganese steel provides the toughness needed under heavy impact conditions. For a

welding alloy it should carry less carbon than cast or wrought shapes, and should include compensating alloy additions.

17. Welding at the minimum practical temperature is highly desirable for manganese steel.

18. Arc power should be kept low and weaving beads of large cross-sectional area should be employed in welding with manganese steel electrodes. Burn-off rate may provide an index of arc power in the absence of adequate instrumentation (Fig. 32).

19. Machine parts are a good field for using martensitic irons to protect against light impact.

20. Where medium impact is involved the trial of martensitic irons, to see if they will stand the impact well enough to realize their good abrasion resistance, is frequently advisable.

21. When hard facing mining, quarry and earth-moving equipment against impact, consideration should be given to properties of the rock being handled (Table 11).

22. Composite structures, using hard facing in a careful pattern, can provide functional efficiency as well as better life in service involving impact and abrasion.

ACKNOWLEDGMENT

The valuable assistance of H. J. Chapin, John W. Gleassey, R. C. Carruthers and Harry D. Sweeney, as well as the Metallurgical Dept. personnel of the American Manganese Steel Division and the American Brake Shoe Co., is acknowledged with appreciation. The author is also grateful to the American Brake Shoe Co. for provision of the necessary research facilities and permission to publish this paper.

Bibliography

1. Avery, Howard S., "Hot Hardness of Hard Facing Alloys," THE WELDING JOURNAL, **29** (7), 552–578 (1950).
2. Hibbard, H. D., "Manufacture and Use of Alloy Steels," *U. S. Bureau of Mines Bull. 100* (1915).
3. Alden, C. A., "Results of Wheel Impact on Crossings." Report by Chief Engineer, Frog and Switch Division, Steelton Plant, Bethlehem Steel Co. Prepared in collaboration with and with the approval of the members of the Manganese Track Society's Standardization Committee.
4. Timoshenko, S., *Theory of Elasticity*, McGraw-Hill Book Co., Inc., New York, pp. 384–385 (1934).
5. Avery, Howard S., "The Metallurgical Background for Welding Austenitic Manganese Steel," American Brake Shoe Co., New York City (1949).
6. Avery, Howard S., "Some Characteristics of Composite Tungsten Carbide Weld Deposits," THE WELDING JOURNAL, **30** (2), 144–160 (1951).
7. Avery, Howard S., "The Selection of Hard-Facing Alloys," American Brake Shoe Co., New York (1951).
8. Yanick, J. S., "An Improved Nickel-Chromium Hardened Chilled Cast Iron," *Trans. A.I.M.E.*, **105**, 53–76 (1933).
9. U. S. Patents 1,988,910 and 1,988,911.
10. U. S. Patent 2,184,518.
11. Avery, Howard S., "Hard Surfacing by Fusion Welding," American Brake Shoe Co., New York (1947).
12. *Standard Specifications for Austenitic Manganese Steel Castings*, ASTM Designation A128–33, ASTM Standards, Part 1, 815–817 (1949).
13. Avery, Howard S., "Austenitic Manganese Steel," American Brake Shoe Co., New York City (1949).
14. *Welding Handbook*, 3rd ed., Chapter 31B, AMERICAN WELDING SOCIETY, New York, pp. 672–686 (1950).
15. "The Statistical Part in Welding Investigations," THE WELDING JOURNAL, **28** (10), Research Suppl., 449-s to 461-s (1949).
16. Ertl, Wagner and Burgh, "Development of a Successful Hard-surfaced Bit for Drilling Oil Shale," U. S. Bureau of Mines Report of Investigations R. I. 4177 (January 1948).

Discussion by C. E. Rogers

Mr. Avery's report on "Hard-Facing for Impact" has many admirable qualities and is indicative of of much time, effort and thought on a subject which heretofore has been quite neglected.

We feel, however, that since Mr. Avery has chosen to include manganese steel as one of the materials examined to collect data for this report, that the title "Materials for Wear Resistance Involving Impact" would have been more appropriate.

Manganese steel is quite well known as an extremely useful material especially under conditions involving wear and impact, but as such it is strictly a base metal, a joining metal or a build-up material and its only connection with hard facing is limited to such functions.

Mr. Avery would extend the term hard facing to include any or all metals applied to the surface of another metal, whether it be similar or dissimilar, for the purpose of resisting some form of wear, or, as in the case of manganese steel, primarily, impact. The designation of manganese steel as hard facing is not in accordance with the facts nor does it conform to the generally accepted definition of hard-facing materials.

We maintain that hard facing or hard surfacing is a process unique in itself and is, by its intended purpose, entirely dissimilar to other processes involving the surface applications of filler metals.

We would like to point out that any satisfactory definition of a hard-facing material should convey the idea of an overlay usually applied by welding processes, with relatively high hardness in the as-deposited condition and without any further treatment whatsoever, such as heat treatment, work hardening, peening or the like which might be required, to develop its useful properties.

Even castings made of hard-facing alloys though they might be used as overlays in the sense of being shrunk or welded in place on shafts, for example, are excluded from this definition.

This definition of hard facing is our own, but is based on common usage, general acceptance and metallography. It indicates a definite process employing specific materials which after all are the general factors normally considered in defining anything.

If Mr. Avery were to use the data applicable to manganese steel as a basis of comparison for rating hard-facing alloys we would concur, since, because of its long and widespread usage in wear-resistant applications involving impact manganese steel would serve admirably as a standard for such comparison.

Mr. Avery dismisses the cobalt base alloys as unnecessarily costly and therefore not to be considered. Cost in hard facing is not always a factor since the

C. E. Rogers, Stoody Co. Whittier, Calif.

replacement of the base material is sometimes far more costly than an application of hard facing in the critical wear areas and if replacement becomes necessary lower cost base metals may be used satisfactorily with proper surface protection.

The austenitic high-chromium, high-carbon irons have not been considered in this report which is regrettable since they are representative of a distinctive hard-facing alloy group, and have performed economically and with great merit on various types of crushing equipment. We assume that Mr. Avery plans to evaluate these and other materials in later reports.

Discussion by Theodore Gaynor

Mr. Avery is to be congratulated on presenting a valuable contribution to the meager information that has been published on the impact resistant properties of hard-facing alloys. "Hard Facing for Impact" is a worth-while supplement to the Author's previous publications on abrasive wear, hot hardness of hard-facing alloys and many others related to the hard-facing field.

The evaluation of welding alloys to withstand light and medium impact is based primarily on compressive strength or resistance to crack formation. The specimens that were used for the laboratory experiments were tested until crack formation occured. The martensitic irons cracked readily; the martensitic steels withstood impact energies up to 50,000 ft.-lb., although the author is of the opinion that these would have failed eventually. The austenitic manganese steels are not expected to fail by crack formation under prolonged laboratory testing.

Of course, there are many applications for hard facing to withstand impact where the weld deposit must be maintained free from cracks as well as all welding defects. Such items as mill buttons, guides, rollers and the like, where impact as well as abrasive wear are operating factors, must be maintained free from defects so as not to mar the surface finish of the hot-rolled product. These operations are light impact operations; martensitic irons as well as high-chromium irons, applied with the torch, have been used successfully.

Mr. Avery recognizes the fact that crack formation may not be a justifiable criterion for evaluating the serviceability of a hard-facing deposit. One of the purposes of this discussion is to emphasize this point with a few illustrations.

A martensitic steel has been used for the cutting edges of shear blades, both for hot shearing large billets and blooms and for cold shearing of rolled plate up to $^1/_2$ in. in thickness. The martensitic steel is applied usually on a 1045 base, but we have had occasion to apply it to 1060 and 1090 steel. The depth of the deposits average $^1/_8$ in. All applications are performed with the manual electric arc. Our original welding technique was designed to minimize or prevent weld cracking by suitable high preheat and interpass welding temperatures, coupled with controlled cooling after welding. The first set of these blades contained only a few widely scattered weld cracks after welding along the 4-ft. surface of the blades' cutting edges. The set was installed in an outdoor shear, completely unprotected from atmospheric temperature changes. The blades were installed during the winter season, and are used to shear hot billets up to 4 x 4 in. square. Because of the tremendous compressive stresses and the frequent temperature changes, cracks developed along the entire weld area. The cracks were perpendicular to the weld bead and were spaced an average of $^1/_2$ in. apart. This set of blades has been in service over two years and has sheared over 170,000 tons of hot steel. No spalling has occurred; the cutting edges are still in excellent condition for shearing.

Our second set of shear blades was fabricated with little regard to preheat, interpass temperature or cooling rate. A similar martensitic steel was applied. After welding, the deposited metal contained numerous transverse cracks. This set of blades was installed in a shear cutting 8- x 8- in. hot blooms. Up to Oct. 1, 1951, over 970,000 tons of blooms have been sheared. The number of weld cracks increased with no apparent loss of efficiency.

We have experienced similar results on other hot shearing applications and on cold shearing applications, although in the latter instance the weld cracks are kept to a minimum during welding.

Of course, because of the relatively tough base materials used for these blades, the weld cracks do not propagate rapidly and cause complete blade failure. Good fusion to the base metal also prevents the cracked areas from spalling. Eventual failure does occur due to wear and plastic deformation. We would not expect martensitic irons to withstand the high compressive strains; it may be noted in passing that cobalt base materials are effective in similar hot applications except that more rapid failure occurs because of plastic deformation.

Viewing this hard facing application in retrospect, except for the crack formation, our choice of the martensitic steel complies with the recommendations offered in Mr. Avery's paper, and fully substantiates these recommendations. Although the velocity of impact is negligible, the compressive loads in the shearing operation are extremely high, which would eliminate the martensitic irons because of brittleness and the austenitic manganese steels because of the requirements for dimensional stability.

Our experience with austenitic manganese steel has been extensive. It has been used for many years as overlays on large mill coupling boxes, pinions, spindles and other items working under heavy impact loads. Contrary to the Author's recommendation, the austenitic steel has been applied generally on a plain carbon steel

Theodore Gaynor, Welding Engineer, Bethlehem Steel Co., Johnstown, Pa.

base. The embrittlement cracks described in the paper have been observed, but with little apparent detriment to the serviceability of the hard-faced item. Of course, in the items noted above, the stresses are applied over a relatively large surface area; the impacted areas are usually completely encased one within the other, with little or no edge effect. We agree that the cracks are harmful when preparing joints subject to tensile or bending stresses in addition to impact and abrasive wear. We have had little success in attempting to butt weld cast manganese steel sections to a hardenable steel base for digging applications, using an austenitic manganese steel filler electrode. The procedure used in surfacing the pearlitic steel base materials calls for at least four layers of the austentic manganese steel welding electrode, so as to assure an undiluted weld deposit on the working surface. Our results with this technique and material have been good. In applications involving severe abrasion, it has been made standard practice to protect the austentitic manganese steel with an overlay of martensitic iron or steel, depending on the impact stresses. By so doing, the service life of manganese steel bucket lips and teeth have been increased by factors of two to four.

We are not one of the school of hard-facing consumers that agree with the contention that the only reliable source of information for general usage is a practical field test. Mr. Avery's evaluation of the three materials on the basis of laboratory impact tests is sound and reveals the nature of the loads causing weld failure, and the resistances of the various materials to these loads. Whether or not the type of failure on which these evaluations are based would prove detrimental to the serviceability of a hard-faced machinery part is questionable; too many entirely local factors enter into the problem. We do believe, however, that with more publications of this nature, hard facing by welding will be put on a firm scientific foundation, instead of the "hit and miss" process it has been in the past.

Author's Reply

We appreciate these discussions and especially welcome the additional field test data that Mr. Gaynor has supplied. His remarks about cracks not rendering overlays unserviceable are very pertinent. We tried to include freedom from cracking of a 150,000 psi. yield strength material as a tentative criterion of light impact. This does not make such alloys unsuitable for medium impact service even though they are brittle. When cracking is not detrimental, as is the case in many applications, it frequently is possible and desirable to select the material on the basis of its resistance to other wear factors. It is noteworthy that hard facing on a tough base makes this tolerance for cracks possible. Complete parts of hard alloys, as represented by white iron castings and the like, are likely to fail completely if a crack starts, whereas in an overlay, the crack generally stops when it reaches the base.

The tolerance for cracks is something that is a matter of engineering judgment on the part of the user. Unfortunately this cannot be handled by test data, as we have studied other properties. Because of this, case histories as cited by Mr. Gaynor are particularly valuable.

The use of manganese steel as a protective overlay on plain carbon steel, as described by Mr. Gaynor, is not hard facing according to Mr. Rogers. We should like to remain open-minded on the rigorous definition of "hard surfacing." We partially agree with Mr. Rogers' opinion of manganese steel in the field of abrasive wear; we consider it better as a base than as an overlay. Nevertheless, many engineers use it for surface protection against wear. In Europe it probably accounts for about half of what they consider hard facing. Perhaps it is significant that the Europeans frequently refer to this austenitic alloy as "hard manganese steel."

On the other hand, when hard is used as a synonym for durable (as derived from the Latin word for hard) the overlays of manganese steel intended to take metal to metal wear would seem to qualify. Mr. Gaynor has listed a number of these, which are associated with heavy impact.

Our approach to industrial wear problems has been aided by the broader concept of durability, and we would be tempted to define hard facing as an overlay technique for extending the life expectancy of mechanical equipment. This would sometimes include corrosion resistant alloys, which in Mr. Rogers' opinion would take us even farther afield. However, there are alloys now sold as hard-facing materials that resist corrosion well but are low in hardness. Others are high in hardness but poor in wear resistance under some conditions. A rigorous definition would encounter so many exceptions that we doubt its present advisability.

Because some available less expensive materials will do a better job under ordinary impact conditions, the cobalt base alloys were not featured here. Their virtues are clarified better in a previous paper on hot hardness.[1] They should be selected primarily for service above 1200° F. and as hot wear is not covered here, they were considered generally inappropriate for this paper though comparative data appear in Figs. 10 and 17.

The austenitic high-chromium irons mentioned also appear in Figs. 10 and 17. Their low yield strength is one reason for giving them scant attention for light impact and their brittleness is a liability under heavy impact. Under medium impact, when cracks are tolerable, they may perform satisfactorily, but even here the martensitic irons are considered a better choice. The chromium irons are excellent for erosion resistance, with little or no impact; and for oxidation resistance up to 1800° F. As Mr. Rogers suggested, we plan to cover them in more detail in a later paper.

PRACTICAL WELDER AND DESIGNER

The Horton Multisphere

by Christian Arne

THE first published article describing the Horton Multisphere, appeared in *The Water Tower*, March 1950, and is reproduced herewith.

"A Multisphere is a pressure vessel made up of two or more spherical sections with one or more internal diaphragms to take the component of the stresses in the shell where the adjoining sections come together.

"The shell of a Multisphere consists of a series of spherical segments. Each segment intersects the adjoining segment in a common plane, and a diaphragm consisting of a flat plate is placed in each plane thus formed.

"The shell of a sphere subjected to an internal gas pressure is stressed in equal biaxial tension. This same stress condition occurs in both the shell and diaphragm plates of a Multisphere. Because of this fact, spheres and Multispheres will withstand twice the pressure of a cylindrical vessel of the same wall thickness.

"Another characteristic that Multispheres for the storage of gas have in common with spheres is that, within practical construction limits, their weight per unit of volume is constant, regardless of size or pressure. In other words, the weight of the metal needed to provide pressure storage for a given volume of gas is the same whether you use a single sphere, two or more spheres, a single Multisphere, or two or more Multispheres. This being true, spheres and Multispheres not only provide an economical means of building pressure containers but also allow variations in design to utilize available plate widths, desirable plate thicknesses, and shop assembly or subassembly. They also make possible the use of units that come within weight and handling limitations, make full use of shipping clearances and utilize space to best advantage.

"An appreciable portion of the total weight of a Multisphere is made up of flat plates. For example, the flat plates in a two-lobe unit—the simplest form of Multisphere—account for 11% of its total weight. In a Multisphere 10 spheres square and one sphere high, 50% of its weight is made up of flat plates.

"Each seam in the shell of a Multisphere is a single weld joining two spherical segments and the edge of an internal diaphragm. All internal joints in diaphragms are three-way or four -way welds.

"Multispheres are patented structures. They can be used to store liquids as well as gases under pressure. Openings in the internal diaphragms permit the contents to flow from one lobe to another. They also equalize the pressure throughout the structure."

Figure 1 shows a 2 × 2 × 9 Multisphere. This vessel has the following properties:

Christian Arne, Research Engineer, Chicago Bridge & Iron Co., Chicago, Ill.

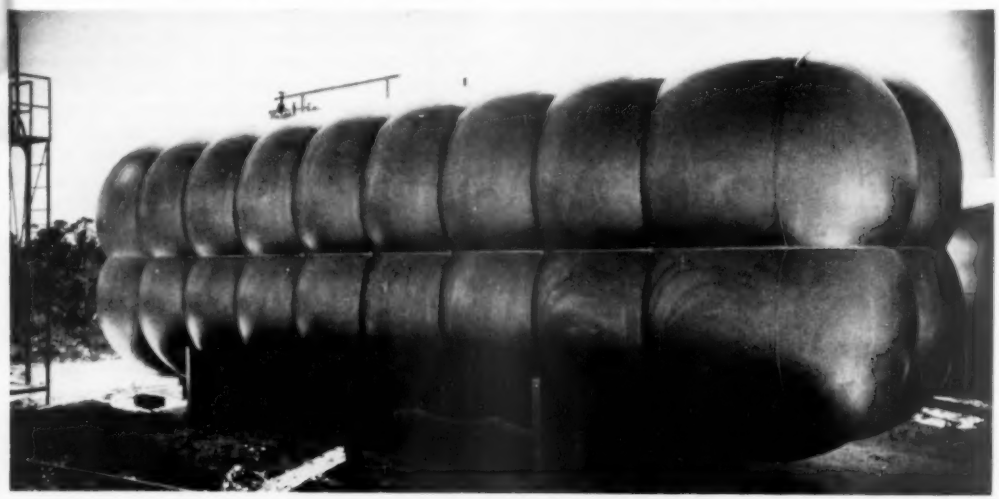


Fig. 1 30,000-gal. Horton Multisphere

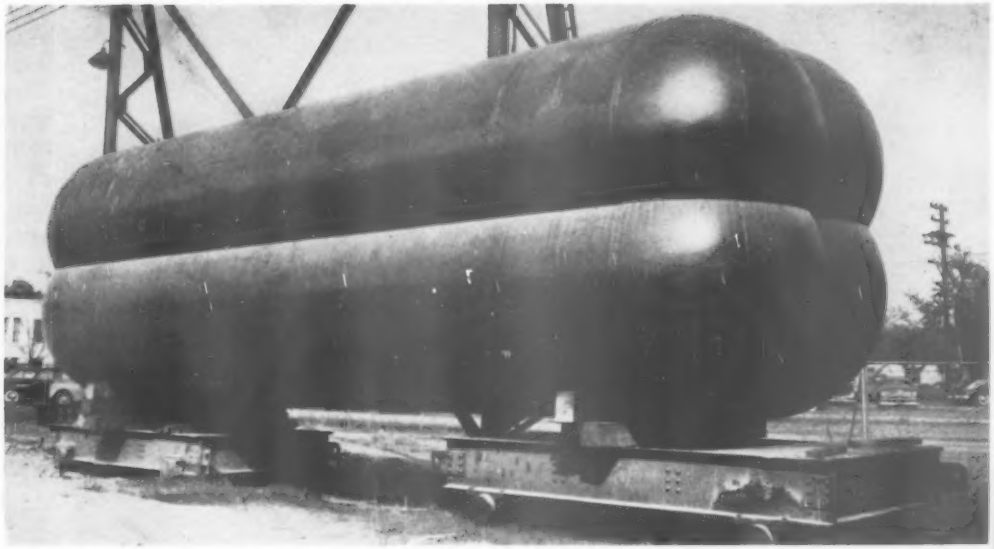


Fig. 2 30,000-gal. Multicylinder

Capacity, gal.	30,000
Weight, lb.	60,000
Height and width, inside	11 ft. 6 in.
Length, inside	38 ft. 4 in.
Spherical radius of each segment	3 ft. 10 in.
Working pressure, psi.	250
Hydrostatic test at, psi.	400
Hammer test at, psi.	312
Shell and diaphragm plates, thickness in in.	0.42

The steel used is A212B with a minimum ultimate tensile strength of 70,000 psi. The design is based on a safety factor of 4 and a joint efficiency of 80%.

This Multisphere is in service at the Superior Dade Gas Corp., Fort Lauderdale, Fla., and is used for the storage of propane. The tank is supported on two steel saddles which are placed under the vertical diaphragms and are set on concrete foundations.

Figure 2 shows a 2 × 2 Multicylinder, having the following properties:

Capacity, gal.	30,000
Weight, lb.	76,000
Height and width, inside	11 ft. 3 in.
Length, inside	37 ft. 10 in.
Working pressure, psi.	250
Hydrostatic test, psi.	400
Hammer test at, psi.	312
Cylindrical shell plates, thickness in in.	0.82
Spherical shell plates, thickness, in in.	0.41
Flat diaphragm plates, thickness, in in.	0.82

The steel used is A212B with a minimum ultimate tensile strength of 70,000 psi. The design is based on a safety factor of 4 and a joint efficiency of 80%.

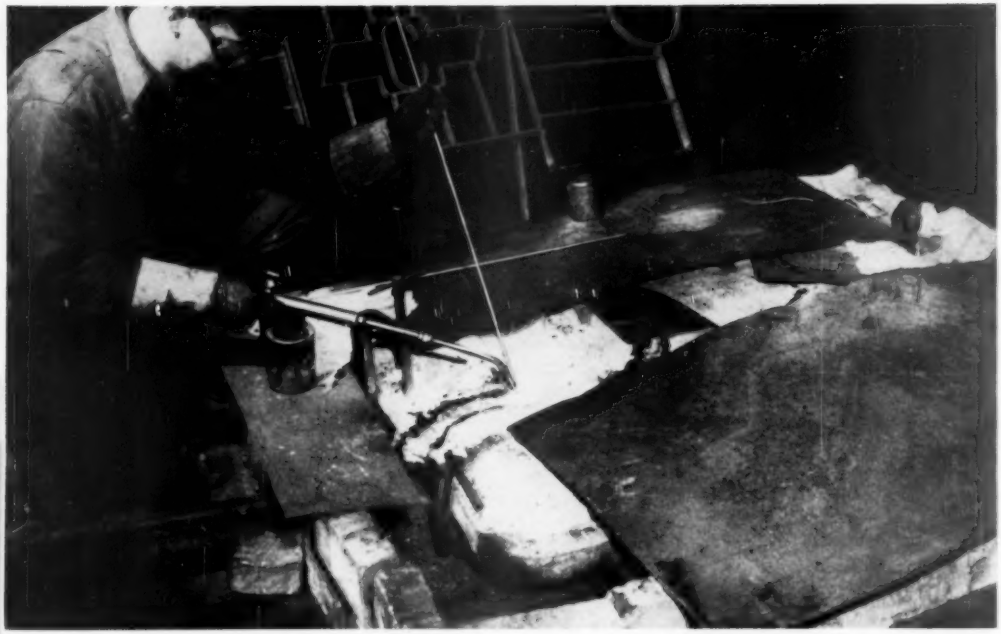
This Multicylinder is in service at the General Natural Gas Co., Salisbury, N. C., and is used to store propane. The tank is supported on two concrete saddles which are formed to bear on the two lowermost cylinders.

Note that the Multisphere and the Multicylinder are both built of the same kind of material to the same specifications with the same capacity and working pressure and both store the same kind of product, but the Multicylinder weighs 1.27 times as much as the Multisphere.

Both these tanks are designed and built by the Chicago Bridge & Iron Co. at Birmingham, Ala. The tanks were ordered by the Warren Petroleum Corp.

WEAR YOUR A.W.S. EMBLEM

It signifies that you contribute to the advancement and perfection of welding through active participation in the American Welding Society. Emblems available through A.W.S., 33 W. 39th St., N. Y. C., at cost.



How to Braze Weld Cast Iron

by H. D. Drummond

SOME cast-iron parts have to be repaired by fusion welding. That is if the part is to be used at temperatures above 500° F., or if the weld must match the color of the casting. But nine out of ten cast-iron jobs can be done by braze welding. It is a fast, easy way to make repairs.

H. D. Drummond is connected with The Linde Air Products Co., Pittsburgh, Pa.

WELDING SMALL PARTS

The first step in any braze-welding job is to prepare the parts for welding. This means thorough cleaning, and usually, beveling broken edges. To repair a part like the broken lever shown in Fig. 1, bevel the edges to make a 90-degree angle. Remove metal from both edges with a file or on a grinder. This will make a vee as shown in Fig. 2, so you can add weld metal. Clean the metal with a stiff wire brush for about one inch on each side of the break.

Fig. 1 Braze welding is a fast, easy way to make repairs. Parts can be welded and put back to work—fast

Fig. 2 Prepare the parts for welding. Bevel both edges so that they make a 90-degree angle like this

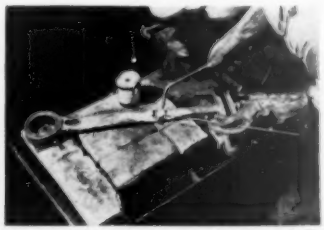


Fig. 3 Heat the part only to a dull red. If the part is too hot the molten bronze will boil

FIT PARTS TOGETHER

Next fit the broken ends together and support them so they do not sag. Connect a medium size welding tip to your blowpipe and get out 1/8-in. flux-coated bronze welding rod. If you do not have flux-coated coated rod, you can use bronze welding rod and Brazo flux. However, it's easier to do braze welding with a flux-coated bronze rod. Since the correct amount of flux is already on the rod, you can deposit the rod metal faster and easier. Because it is just a little more difficult to use the bare rod and can of flux, the photographs show the job done that way.

Adjust the blowpipe for a slightly excess oxygen flame, and heat the lever on both sides of the break. Now direct the flame at one end of the bevel. When it turns dark red, place the end of the rod into the flame and then on the hot metal. The bronze rod should melt.

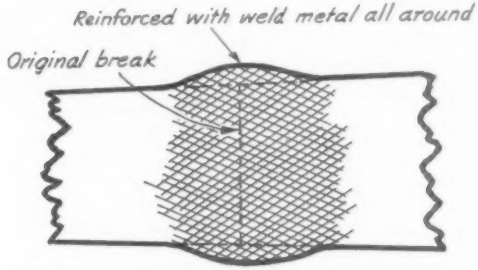


Fig. 4 Add enough rod to the vee so that it is built up slightly higher than the surface of the part. Be sure to support the part so that it does not sag during welding. Here is the finished lever

GOOD TINNING IMPORTANT

If you did a good cleaning job, molten metal from the rod will flow smoothly onto the heated metal and down the vee like water on clean glass. This is good tinning. If the part was heated too much, the molten bronze will boil and run around like water on a hot stove. If the part was not heated enough, the molten bronze will form small drops and will not tin. And good tinning is needed for a good weld.

BUILD UP WELD

When the molten bronze starts to tin, keep adding bronze to the vee until it is built up slightly higher than the surface of the lever. Note that the flame melts the rod and keeps the puddle at the right temperature. It also preheats the metal ahead of the puddle and prepares it for tinning.

WELDING ACROSS VEE

When you build up the weld, start moving the blowpipe forward slowly. The puddle will move too. As it moves along, the metal at the rear will harden. Raise and lower the blowpipe flame to keep the metal and puddle at the proper temperature. When the metal ahead of the puddle tins automatically, it is the right temperature. As the flux on the rod is used up, dip the rod into the can of flux.

Add welding rod by keeping the rod in the puddle. Raise the rod and draw back the flame if the puddle gets too big. Move the welding rod around in the puddle a little to get an even deposit of bronze. When you complete the weld, let it cool. Then grind the weld smooth.

WELDING LARGE PARTS

Large cast-iron parts need more preparation. To

Fig. 5 Edge preparation, exact line-up, and proper preheat are key factors when making repairs on large parts like this cast-iron engine cylinder

Fig. 6 Prepare the part by veeing out the crack and broken edges to a 90-degree angle. Use a cold chisel and hammer. The small piece can be beveled on a grinder

Fig. 7 Proper preheat is important when welding cast iron. Preheating will equalize stresses that might crack the casting during welding or while cooling

make repairs on parts like the engine cylinder shown in Fig. 5, edge preparation, exact lineup, and proper preheat are key factors.

The first step is to vee out the crack and broken edges to a 90-degree angle (see Fig. 6). Use a cold chisel and hammer to vee out the crack. The small piece can be beveled on a grinder. Go over the edges with a file and stiff wire brush to remove any chips or particles picked up from the grinding wheel.

MAKE TACK WELDS

Next fit the small part in place. Use supports to hold the broken section while you make tack welds. First, make a tack weld at the inner end. Then make one at the end of the cylinder.

After you make the tack welds, check the alignment of the broken section with the casting. It is best that the parts line up exactly so that after welding, only slight machining will be necessary inside the cylinder. When it fits exactly, make a third tack weld at the end of the cylinder where the long crack is.

PREHEAT IMPORTANT

When welding cast iron, proper preheat is important because it equalizes the stresses that might crack the casting during welding or while cooling. It also relieves stresses that may exist in the casting.

Even though this is a braze-welding job, and does not require as much preheat as you would use for fusion welding, the casting should be preheated carefully. The welding heat should not be too great around any area, since stresses may be set up in the casting during welding. And any stresses set up during welding might crack the casting more. Preheating will help keep the welding heat even, and eliminate the possibility of further cracking. It is best to build a temporary fire-brick furnace and use charcoal to do the heating for this job. However, on some jobs the preheating can be done with the blowpipe.

Build a furnace and place the cracked end of the casting into it (see Fig. 7). Add two or three shovelsful of charcoal evenly around the casting, then light it with the blowpipe. Do not use kerosene, or anything else — the oxyacetylene flame will start the fire itself. Cover the end of the casting with asbestos paper or steel sheathing. Mark the casting with an 800° F.-temperature-indicating crayon or pellet to tell you when the casting is ready for welding.

BEGIN WELDING

Be sure to place the casting in the furnace so that it is easy to make the weld.

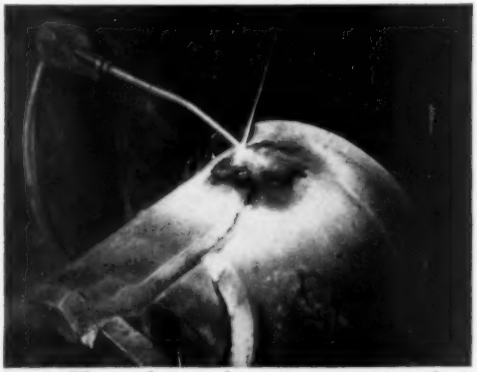


Fig. 8 When preheat reaches proper temperature, begin welding. Use an excess oxygen flame, and direct it on the end of the bevel. When the spot on the bevel turns dark red, place the heated end of the rod on it

Fig. 9 When you finish the weld, cover the casting with asbestos paper to keep off drafts. The completed job should look like this. It is as strong as the original metal because proper preheat and welding procedure was used

When the pellet melts, begin welding. Use an excess oxygen flame, and start the weld at the innermost point of the crack. Direct the flame at the end of the bevel. Heat the end of the bronze rod in the flame. When the spot on the bevel turns dark red, place the rod on it. It should melt like it did when you began welding the small part. A flux-coated bronze welding rod will speed this job, too.

When the molten bronze begins to tin, add welding rod to the puddle. Work toward the edge or end of the cylinder. When you reach the end, go back to the innermost point, and make a weld at the other crack. Work toward the edge of the cylinder again. Build up the entire weld higher than the surface of the casting.

When you finish the weld, cover the casting with asbestos paper to keep off drafts. The completed job should look like Fig. 9. It is as strong as the original metal.

(*Continued from page 108*)

Gas Turbines. Welding in Relation of Gas Turbines for Use on Land, J. M. Robertson. Inst. Welding—Trans., vol. 14, no. 3 (June 1951), pp. 68–73.

Glass Bottles, Crates. Metal Crate Production. Welding & Metal Fabrication, vol. 19, no. 10 (Oct. 1951), pp. 379–382.

Heat Exchangers. Brazed Aluminum Heat Exchangers. Modern Metals, vol. 7, no. 9 (Oct. 1951), pp. 25–28.

Hydraulic Turbines, Maintenance and Repair. Water Wheel Repair Time and Cost Cut 50%. Elec. Light & Power, vol. 29, no. 10 (Oct. 1951), pp. 88–94.

Industrial Plants, Power Supply. Electric Distribution Ideas for Welder Production, H. P. Scott. Elec. Construction & Maintenance, vol. 50, no. 10 (Oct. 1951), pp. 104–107, 128.

Iron Castings. How to Fusion Weld Gray Cast Iron. Mill & Factory, vol. 49, no. 5 (Nov. 1951), pp. 104–105.

Jigs and Fixtures, Hydraulic. Long Stroke Shuttling Cylinder Reduces Cycle Time. Applied Hydraulics, vol. 4, no. 9 (Sept. 1951), pp. 52–54.

Light Metals. Recent French Studies on Weldability of Aluminium Magnesium Alloys, J. Herenguel and M. Hollard. Inst. Welding—Trans., vol. 14, no. 4 (Aug. 1951), pp. 140–144.

Locomotive Maintenance and Repair. Reclaiming Aluminum Diesel Locomotive Parts, L. M. Grover and R. L. Rex. Ry. Mech. & Elec. Engr., vol. 125, no. 9 (Sept. 1951), pp. 56–58.

Locomotive Manufacture. Improvement in Production of Multiple Parts by Oxyacetylene Cutting Techniques, H. S. Swan. Industry & Welding, vol. 24, no. 10 (Oct. 1951), pp. 38, 40–41, 112.

Magnesium and Magnesium Alloys. Clad Magnesium, G. W. Orton. Light Metal Age, vol. 9, no. 9, 10 (Oct. 1951), pp. 10–11, 14, 16.

Materials Handling, Dockboards. Weldability of Magnesium Permanent Mold Castings Makes Improved Modern Dockboards Possible, V. J. Daniels. Precision Metal Molding, vol. 9, no. 10 (Oct. 1951), pp. 44–46.

Natural Gas Pipe Lines. Welding Seagoing Pipe Line, R. C. Lesiford and E. Sterrett. Welding Engr., vol. 36, no. 9 (Sept. 1951), pp. 38–39, 106.

Oil Tankers, Maintenance and Repair. Inerting Oil Tankers for Safe Welding, W. Cooper. Welding Engr., vol. 36, no. 9 (Sept. 1951), pp. 56–58, 60, 104–106.

Oxygen Cutting. Flame Cutting Solves Armor-Beveling Problems, R. Sibley. Iron Age, vol. 168, no. 20 (Nov. 15, 1951), pp. 117–119.

Oxygen Cutting. Machine-Cutting in Forge, L. Hodges. Inst. Welding—Trans., vol. 14, no. 4 (Aug. 1951), pp. 145–148.

Oxygen Cutting. Metal Removal by Carbon Arc. Industry & Welding, vol. 24, no. 10 (Oct. 1951), pp. 60–62, 65–66, 111.

Petroleum Pipe Lines, California. Transplanting Old Pipe Line, C. Bronson. Welding Engr., vol. 36, no. 10 (Oct. 1951), pp. 48–50.

Pipe Cutting. Oxy-Acetylene Processes in Pipe Fabrication, E. P. Auler. Can. Machy., vol. 62, no. 10 (Oct. 1951), pp. 177–180, 182, 184.

Plastics. Hot Air "Welds" Plastics, F. Charity. Am. Mach., vol. 95, no. 23 (Nov. 12, 1951), pp. 164–165.

Pressure. Cold Pressure Welding. Mass Production, vol. 27, no. 4 (Apr. 1951), pp. 74–83.

Quality Control. Another Inspection Method for Quality Control. Industry & Welding, vol. 24, no. 10 (Oct. 1951), pp. 43–44, 47.

Railroad Materials, Reclamation. Economization by Standardization of Alloys and by Method of Reclamation of Scrap Metals, C. E. Dinsdale. Metal Industry, vol. 79, no. 20 (Nov. 16, 1951), pp. 419–421.

Refrigerating Plants, Maintenance and Repair. Welding Solves Maintenance Problems, C. W. Brett. Modern Refrig., vol. 54, no. 641 (Aug. 1951), pp. 230–231.

Resistance Welding, H. E. Dixon and H. G. Taylor. Sheet Metal Industries, vol. 28, no. 296 (Dec. 1951), pp. 1121–1130, 1136.

Road Machinery, Maintenance and Repair. Better Welding for Better Equipment Maintenance and Repair, G. L. Wiley. Roads & Streets, vol. 94, no. 6 (June 1951), pp. 34–43.

Rubber Machinery. All Welded Support for Multiple Platen Press, O. E. Hermanns. Rubber Age (N. Y.), vol. 69, no. 6 (Sept. 1951), pp. 707–712.

Stainless Steel. Fabrication and Welding of Austenitic Stainless Steels, P. L. Pocock. Sheet Metal Industries, vol. 28, no. 294 (Oct. 1951), pp. 933–942, 944.

Stainless Steel. Welding Unstabilized Austenitic Stainless Steels Without Carbide Precipitation, J. A. Goodford and D. W. Kaufmann. Matls. & Methods, vol. 34, no. 5 (Nov. 1951), pp. 64–65.

Steel, Corrosion Resisting. Structural Changes Sabotage Corrosion-Resistant Liners, J. H. van Swaal. Iron Age, vol. 168, no. 19 (Nov. 8, 1951), pp. 138–142.

Steel Hardening, Oxygas Process. Flame Hardening Proves Versatile Heat Treating Method, S. Smith. Iron Age, vol. 168, no. 21 (Nov. 22, 1951), pp. 96–98.

Steel Structures, Welded and Cast Combined. Cast-Weld Design. Machine Design, vol. 23, no. 10 (Oct. 1951), pp. 137–40.

Television Antennas. Rotating Mast for Television, J. Turner. Welding Engr., vol. 36, no. 9 (Sept. 1951), pp. 52–53.

Vacuum Cleaners. Redesign to Simplify Tooling, A. F. Murray. Machine Design, vol. 23, no. 9 (Sept. 1951), pp. 153–154.

Water Heaters, Manufacture. Costs Cut by Simplified Welding Operations, H. E. Jackson. Industry & Welding, vol. 24, no. 10 (Oct. 1951), pp. 52–54, 59, 108–109.

Water Pipe Lines, Steel. Fabrication of Water Mains, P. L. Pocock. Water Power, vol. 3, no. 11 (Nov. 1951), pp. 419–423.

Water Pipe Lines. Construction of Australia's Largest Water Pipeline, D. Cooke. Welding & Metal Fabrication, vol. 19, no. 10 (Oct. 1951), pp. 374–378.

Water Tanks and Towers. New All-Welded Steel Standpipe at St. Foy, Quebec, has Million Gallon Capacity, E. Nesbitt. Eng. & Contract Rec., vol. 64, no. 11 (Nov. 1951), pp. 84, 86.

Welded Steel Structures. Developments of Design and Fabrication in Recent British Structures, W. S. Atkins and E. M. Lewis. Inst. Welding—Trans., vol. 14, no. 3 (June 1951), pp. 74–84.

Welded Steel Structures, Stresses. Design and Fabrication of Welded Structures Subjected to Repeated Loading, R. Weck. Welder, vol. 19, no. 105 (July-Sept. 1950), pp. 61–70; vol. 20, no. 107 (Jan.-June 1951), pp. 12–22.

Welding at War. Welding Engr., vol. 36, no. 10 (Oct. 1951), pp. 51–53.

Welding, Gas. Importance of Repairability, G. E. Hammon. Welding Engr., vol. 36, no. 10 (Oct. 1951), pp. 42–44, 82–83.

Welding Light Metals. Methods of Minimizing Cracking at Temperatures Above Solidus During Fusion Welding of Aluminium Alloys by Oxyacetylene and Argon-Arc Processes, D. C. Moore. Sheet Metal Industries, vol. 28, no. 295 (Nov. 1951), pp. 1025–1037, 1040.

Welding Machines, Maintenance and Repair. How to Prevent Arc Welder Troubles. Welding Engr., vol. 36, no. 10 (Oct. 1951), pp. 34–36.

Welding Machines, Maintenance and Repair. Preventive Maintenance for Resistance-Welding Equipment. Welding Engr., vol. 36, no. 10 (Oct. 1951), pp. 338–341.

Welding Machines, Maintenance and Repair. Welding Equipment Maintenance—Arc, D-C. Tips on Troubleshooting, What To Do If—. Welding Engr., vol. 36, no. 10 (Oct. 1951), p. 37.

Welds, Finishing. Clean-Up Is No Longer Bottleneck, E. F. Anderson. Welding Engr., vol. 36, no. 10 (Oct. 1951), pp. 54–55.

Welds, Heat Treatment of Welds in Pipe-Lines, A. H. Goodger. Indus. Heating Engr., vol. 13, no. 69 (July 1951), pp. 218–220.

Welds, X-Ray Analysis. Short-Range Radiography for Weld Inspection, E. H. S. van Someren. Welder, vol. 20, no. 107 (Jan.-June 1951), pp. 10–11.

Crusher Parts Made Tougher by Hard Facing Using Submerged Arc Welding

HADFIELD manganese crusher cones and mantles may be rebuilt and hard-surfaced using the Unionmelt welding process to outlast the original parts, according to the Finning Tractor and Equipment Co., Ltd., of Vancouver, B. C. This company reports that its results have been so successful that hard facing is now also applied to new units to give them 50% more service life than unprocessed parts. Inspection after service reveals little or no deterioration of the hard-faced surface.

New cones and mantles are surfaced by applying a special hard-facing underlay first, and then overlaying with hard-facing rod. For units that have been in service, a preliminary build-up of manganese steel may be required prior to depositing the special hard-facing underlay. If the unit is worn more than $^1/_2$ in., reclamation is not attempted.

The crusher part is first given a preheat from 150 to 200° F. in accordance with standard practice, and during welding the temperature of the part is kept below 500° F. at all times. Frequent checks with temperature-indicating crayons enable the operator to control this important factor. Prolonged heating in the neighborhood of 750° F. would result in embrittling the Hadfield manganese steel.

Courtesy, Linde Air Products Co.

Fig. 1 First step in hard facing a manganese steel crusher cone: application of hard-facing base material, using submerged-arc welding

Figure 1 shows the application of hard-facing base material to a Hadfield-manganese cone prior to final hard facing. Unionmelt composition Grade 80 was used and a welding current of 300 amp. at 30 v. alternating current was maintained with welding speeds of 18 in. per minute. Figure 2 shows the corresponding deposit on the inner face of the mantle. Figure 3 shows a partial coverage of a worn cone surface. Here approximately 70 lb. of underlay and 30 lb. of hard-facing overlay were deposited.

Fig. 2 Crusher mantle shown with hard-facing base material applied, prior to application of finish hard-face layer

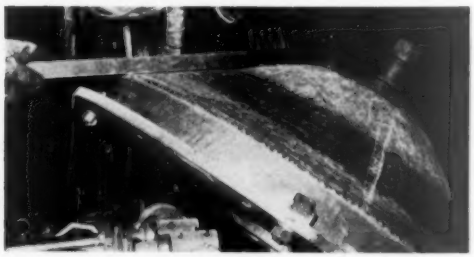


Fig. 3 Final application of hard-facing layer well under way on crusher cone

Aluminum Spot Welding Symposium

The AWS Resistance Welding Committee has been working on Recommended Practices for Spot Welding Aluminum and Aluminum Alloys for the past few years. These Recommended Practices are almost ready to take their place alongside the other Recommended Practices for other metals which were published a few years ago.

Because of the special considerations involved in spot welding aluminum, the Committee is holding a symposium on Tuesday and Wednesday, Mar. 11 and 12, 1952, to discuss the Proposed Recommended Practices and other related aspects of aluminum spot welding for all types of industrial applications. Everyone interested in the subject will be welcome.

For further details as to the exact time and place (Chicago has been tentatively selected) and a copy of the Proposed Recommended Practices write to S. A. Greenberg, Secretary, AWS Resistance Welding Committee, AMERICAN WELDING SOCIETY, 33 W. 39th St., New York 18, N. Y.

First 1951–52 AWS Executive Committee Meeting

The first meeting of the Executive Committee of the AMERICAN WELDING SOCIETY for the 1951–52 year was held in Room 1105, Engineering Societies Bldg., New York, N. Y., on Tuesday, Dec. 11, 1951, at 10:00 A.M. The following attended:

Members: *Chairman* C. H. Jennings, F. L. Plummer, E. R. Seabloom, R. S. Donald, L. C. Bibber and H. E. Rockefeller.

AWS Staff: J. G. Magrath, *Secretary*, and F. J. Mooney, *Assistant Secretary*.

Guests: H. Biers, F. Jonassen and W. Spraragen.

International Institute of Welding

(*a*) The Secretary stressed the fact that, on the basis of European custom, billings for dues in the International Institute of Welding were submitted for the year past, rather than in advance. Accordingly, as of Sept. 26, 1951, the Institute of Welding issued an invoice to the AMERICAN WELDING SOCIETY for dues covering the period Jan. 1 to Dec. 31, 1951. Total dues in American money for American representation in 1950 were $491.65. At the Annual Meeting of the IIW in mid-year 1951, that Institute's governing board voted to raise the dues 50% for the current year. Accordingly, we are invoiced, as of Sept. 26, 1951, for an amount of $737.41. The Welding Research Council pays one-third of these dues, or $245.80, leaving a balance to be paid by AWS of $491.61. The Secretary observed that inasmuch as the budget prepared for the 1950–51 fiscal year provided for payment of but two-thirds of the previous year's amount which was $491.65, AWS's share amounting to $327.77, it was necessary that he receive approval from the Executive Committee to authorize payment of the difference.

ACTION: (*a*) The increased dues for 1951 were accepted and the Secretary was authorized to approve payment.

(*b*) The Secretary then observed that, at this time, after three years of participation, it was advisable that the membership of AWS in IIW be given consideration from the standpoint of whether it should be continued or discontinued. He stated that in order to present the case for the IIW, the Chairman had invited Howard Biers, American Vice-President of IIW, and Dr. Finn Jonassen, Research Coordinator, War Metallurgy Committee, Committee on Ship Steel, National Research Council, who would speak for IIW on the basis of individual membership therein on one of the Research Commissions. Mr. Biers addressed the Committee stating that, first, the IIW wished to extend its appreciation to AWS for the hospitality and assistance provided on the occasion of the visit of IIW foreign members to the AWS Annual Meeting in Detroit in October 1951 on the parallel occasion of the World Metallurgical Congress in Detroit. He next stated that research and other technical activities in welding in Europe were set up on a plan requiring long time activity, over a period of years, and that funds therefor were quite limited. In his opinion, should AWS, at this time, discontinue membership in IIW, AWS would be stepping out of the picture prematurely. He believed that it was still necessary to carry on membership for a while longer, not that in any way or form could he or others guarantee that America would benefit therefrom but rather that it was necessary to assist European organizations during this difficult period, to encourage them in their activities, by participation and displayed interest of AWS and, whenever possible, endeavor to have American representatives attend their Commission and Annual Meeting activities. He pointed out that the major necessity for success was the financial assistance of the United States. He observed the hazard that might develop should AWS withdraw from the activity, such hazard being that without contact through representation, AWS which represents American industry would not be in a position to (1) be aware of trends, and (2) move in approved procedure toward the end of preventing action detrimental to the interests of American codes, standards and specifications' institution.

Dr. Finn Jonassen addressed the Committee stating that one-half of the Commission structure of IIW was devoted to the study and development of "Standardization" and the other half to the study and development of "Research." It had been observed previously by Mr. Biers that the Annual Meeting of IIW in Sweden in 1952 will have as its theme "Welding in Ship Structure." Dr. Jonassen expressed the direct interest of his group in this activity and also stressed that those Americans who had had the chance to confer with the visiting members (at the World Metallurgical Congress) of IIW, as well as those American members who had the opportunity to attend IIW activities in Europe, were most enthusiastic about the good that could be done by IIW and its future. Dr. Jonassen agreed with Mr. Biers that immediate benefits might not be forthcoming but that we in the States would have to consider this from a long-range viewpoint and that it was very necessary that American participation include American representatives attending, in person, the European activities.

W. Spraragen, in the capacity of Director of WRC, advised that WRC also felt that it was advisable that membership be continued for several additional years in order to give the project ample time to develop and to begin to show results. He stated that even if there were no IIW, he did not feel the WRC activities would be hampered in that now, even as prior to the war, close relationship existed between the American institutions and the various European individual country welding organizations. That relationship included not only the exchange of information but of periodicals and such had been found a satisfactory vehicle for dissemination of information on American research and standards. However, WRC felt that possibly much of this could now be coordinated through the IIW. Nevertheless, WRC would be guided by whatever action was taken by AWS and would follow through in the same order.

Dr. Jonassen verbally confirmed the written advice that the Ship Structure Committee was in a position to share one-third membership (one-third by AWS and one-third by WRC) and was willing to have their membership start as of Jan. 1, 1952.

Tempilstiks°

the amazing crayons that tell working temperatures from 113° to 2000° F.

in
welding
flame-cutting
tempering
forging
casting
molding
drawing
straightening
heat-treating
in general

$2 each

gives up to 2000 readings

It's this simple: Mark the workpiece with the proper Tempilstik° When the mark melts, the specified temperature has been reached.

Available in these temperatures (°F)							
113	200	288	375	630	1000	1350	1700
125	213	300	388	700	1050	1400	1750
138	225	313	400	750	1100	1450	1800
150	238	325	450	800	1150	1500	1850
163	250	338	500	850	1200	1550	1900
175	263	350	550	900	1250	1600	1950
188	275	363	600	950	1300	1650	2000

Also available in pellet and liquid form

FREE SAMPLE PELLETS

Use this coupon. Specify temperatures of interest to you.

TEMPIL° CORP., 132 West 22nd St., N. Y. 11, N. Y.

Please send free sample pellets in the following rating_____

Name_____

Company_____

Address_____

INDEX TO ADVERTISERS

Air Reduction . . . . . Outside back cover
All State Welding Alloys Co., Inc. 174
Alloy Rods Company 159
The American Brass Co. . . . . Inside back cover
American Chain and Cable Co. 162
American Manganese Steel Division 163
Ampco Metal, Inc. 178
Anti-Borax Compound Co. 160

Bastian-Blessing Co. 184
Burdett Oxygen Company 175

Dockson Corporation 178

Eastman Kodak Company 171
Eutectic Welding Alloys Corp. 176

Fansteel Metallurgical Corp. 186

General Electric 155, 169, 177

Harnischfeger Corporation 165
Haynes Stellite Company, a Division of Union Carbide and Carbon Corporation 179
Hobart Brothers Company 91

The International Nickel Company, Inc. 96

The Lincoln Electric Company 173
Linde Air Products Company, a Division of Union Carbide and Carbon Corporation 94

P. R. Mallory & Co., Inc. 93
Metal & Thermit Corporation 92

National Carbide Company 156
National Cylinder Gas Company 182, 183
National Welding Equipment Co. 157

Page Steel and Wire Division 162

Sight Feed Generator Co. 180
Smith Welding Equipment Co. 172
Square D Company 167
Stulz-Sickles Co. 180

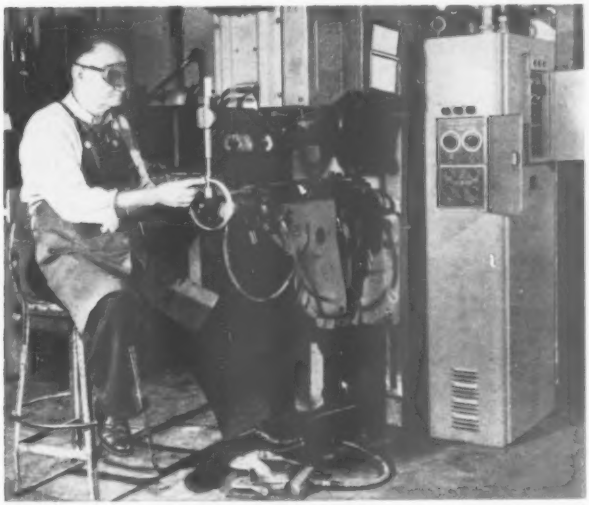
Tempil° Corporation 154
Tweco Products Company 166

Union Carbide and Carbon Corporation
Linde Air Products Company 94
Haynes Stellite Company 179
United States Testing Company Inc. 158

Victor Equipment Company . . . . . Inside front cover

Wagner Manufacturing Co. 186
Westinghouse Electric Corporation 160
J. H. Williams & Co. 158

Solar Aircraft Welds Close to the Edge



Material does not split out, spattering is reduced on this jet engine part because G-E slope control permits a gradual increase in welding current that also reduces tip pick-up and spatter. More welds can be made before electrodes must be cleaned. Welds are sound and uniform. Bulletin GEC-534.

G-E Slope Control for Resistance Welding Prevents Split-outs on Stainless Steel Jet Engine Parts

More precise work possible with this resistance welding accessory used with G-E synchronous control.

Solar Aircraft, like many other plants working on jet engines, has found G-E synchronous control, with slope control added, will enable operators to work to closer tolerances, produce faster, with fewer rejects. The part shown is welded close to the edge but does not split out, and spatter is reduced on both stainless and mild steel.

Use G-E Synchronous Control wherever AN-W-30 and 32 specifications must be met. It assures consistently uniform high quality welds—operates quietly, requires little maintenance. Like all G-E electronic equipment, it has long life, is enclosed in a compact unit that may be mounted on the welding machine or wherever convenient. Easily inspected. Write today for Bulletin GEA-4699. *General Electric Company, Schenectady, N. Y.*

Be sure of consistent, high-quality welds with these G-E Accessories

DETERMINE THE BEST TIMING FOR A JOB **Portable, inkless G-E cycle recorder makes a record of the exact timing on test welds, is then used as a check to verify the setting on machines in production. Bulletin GEC-376.**

HOLD WELDING CURRENT CONSTANT **Regardless of line-voltage variations of as much as plus 10 per cent and minus 20 per cent, the G-E electronic voltage-regulating compensator holds welding current constant. Bulletin GEA-4223.**

REDUCE BRITTLENESS **Heat treat medium-carbon, low-alloy, or high-alloy steel with G-E tempering control. Easily installed and operated. Adjustable to suit thickness and type of metal welded. Bulletin GEA-4201.**

MEASURE ELECTRODE FORCE **Check existing gages on spot, seam, or projection welders or at time of set up. Easy to use, saves time, acts as a production check. Force range: 0 to 4500 pounds. Small, portable. Bulletin GEA-36288.**

PREVENT CURRENT VARIATIONS **Where the insertion of magnetic material in the throat of the welding machine causes weld variations, the current-regulating compensator keeps current constant to within plus or minus two percent. Bulletin GEA-4207.**

GENERAL ELECTRIC

615-54

Chairman Jennings observed that in our experience in the past, and even now, it was very difficult to interest industry in financing the cost of American representation on the various Commissions attending the European activities. There just was not that much interest on the part of American industrial management.

The Secretary stated that when the IIW invitation was received by AWS several years ago, it was first presented to the Technical Activities Committee. That Committee rejected the proposition from the standpoint of their handling the activity. He observed that, as it had been pointed out in the preceding discussion at this meeting, one-half of IIW activities was Standardization and one-half Research. Accordingly, he did not feel that the activity, if it were to be continued by AWS on the basis of AWS being the American Secretarist, should be handled by the Business Secretary of the Society. Other ways and means should be found. He suggested the possibility of the Ship Structure Committee's interest being strong enough for them to handle the Secretaryship. However, they pointed out that their interest was tied in with WRC mostly on Research and that consideration had not been given to handling the Secretaryship for any part of the activity; their offer to participate being based upon representation on the basis of one-third payment of the American dues. In cross discussion, it was determined that the matter of final disposition of Secretaryship should be left to the American Committee on IIW, that Committee to study and report thereon to AWS Board of Directors and the other interested parties, WRC and the Ship Structure Committee, if they participated in membership.

Action: (*b*) It was unanimously voted to continue with IIW for the year 1952.

Action: (*c*) Upon motion, duly seconded, it was unanimously voted that AWS approve acceptance by the American IIW Committee of the Ship Structure Committee's offer to participate in American membership in IIW to the extent of one-third monetary interest.

Recommendation: Chairman Jennings recommended to the American IIW Vice-President, Howard Biers, that when attending future European activities of IIW, he stress to IIW governing board the necessity of maintaining the present dues level and, if changes were necessary, to provide advance information thereon prior to making the change so that the American representation would be in a position to express its opinion.

Special Committee Appointment

Special Committee to Determine Scope, Purposes and Requirements of AWS Annual Meeting: The Secretary advised of the resignation of H. W. Pierce as Chairman of this Committee, such resignation being due to his inability to give this Committee the time required as a result of extraordinarily heightened activities and duties in his business organization. The Chairman reviewed briefly the need of having a high Society Officer preside over this activity and, accordingly, he recommended the appointment of F. L. Plummer, First Vice-President.

Action: The Chairman's recommendation of Mr. Plummer as Chairman of this Committee was unanimously approved.

Special Committee Appointment

Special Committee on Welder Certification: The Chairman advised that F. L. Plummer, due to his taking over the extensive duties of Chairman of the Special Committee to Determine Scope, Purposes and Requirements of the Annual Meeting, was requesting the acceptance of his resignation as Chairman of the Special Committee on Welder Certification, with the suggestion that someone else be appointed to carry on this activity, such person to be an Officer or Director of the Society. The Chairman stated that he was appointing E. R. Seabloom, Second Vice-President, as Chairman of the Special Committee on Welder Certification and was now submitting his recommendation to the Executive Committee for approval.

Action: It was voted to accept the resignation of Mr. Plummer as Chairman of the Special Committee on Welder Certification and the appointment of Mr. Seabloom as Chairman of this Special Committee was voted approval.

The meeting adjourned for luncheon at 12 noon; reconvened at 1:50 in Room 608.

Student Chapters

The Secretary advised the Board of Directors that at its meeting on Aug. 28, 1951, as a result of the suggestion that some form of Vocational Student Membership and Vocational Student Chapter provisions be incorporated into our By-Laws, the Board tabled the item pending Execu-

National
Carbide
IN THE RED DRUM
EFFICIENT
ECONOMICAL
DEPENDABLE

FOR WELDING and CUTTING
Use National Carbide in the Red Drum
Write us for information as to nearest available stock.
60 E. 42nd St. NATIONAL CARBIDE COMPANY New York 17, N.Y.
A Division of Air Reduction Co., Inc.

Would you like answers to questions such as these?

- what is gas and how does it behave
- how do gases mix in a torch
- what are the more important flame temperatures
- which is hotter — the oxydizing or carbonizing flame
- why do some flames heat rapidly and others slowly
- can you guess the speed of a gas molecule

Would you like to see full color pictures of seven different flame types?

The answers to these questions are but a sample of the interesting information this booklet brings to you.

Do write now for your *FREE* copy.

NATIONAL WELDING EQUIPMENT CO.
218 Fremont Street, San Francisco 5, Calif.

Please send FREE booklet "you don't need an egg beater to mix gases"

NAME_____

POSITION_____

COMPANY_____

ADDRESS_____

CITY_____ ZONE___ STATE_____

Please print to make sure you receive your copy.

514

Made by National SINCE 1910 WELDING EQUIPMENT CO., San Francisco 5, California

tive Committee action on a survey to be made by the SOCIETY's Secretary of other Societies' Student Member requirements and fees. The Secretary caused a check of other Engineering Societies' dues and a review of these was submitted to the committee. It is to be recalled that the Finance Committee originally recommended a dues increase on regular Student Membership from $3.00 to $5.00 per year. The survey indicates that ASME charges $3.00; AIEE $5.00; SAE $3.00; AIME $4.50; SNAME $3.00; and ASTM $2.00. In only one instance did the survey disclose that a Society had a similar arrangement to Vocational Student Chapters. This is the American Society for Testing Materials, which states that the student "must be an undergraduate or graduate student in a technical school of recognized standing, or a student not over 25 engaged in technical studies in an apprentice course or night school." Accordingly, the Secretary advised that with this one exception, due to the wide difference in activities between the regular engineering societies and the AMERICAN WELDING SOCIETY, in so far as occupational classification of membership is concerned, there is but the one parallel to draw upon. He stated that the SOCIETY now has a request from the California State Polytechnic College at San Luis Obispo, Calif., advising that they wish to start a Student Chapter. If the SOCIETY is to accept only students from and to approve Student Chapters located at accredited colleges, then vocational schools, such as, Le Tourneau, California State Polytechnic and others of the kind, not accredited schools, cannot be recognized. The Secretary again urged the creation of a pattern providing Student Membership for such schools, as well as provision for Student Chapters. Originally, the Finance Committee determined that the cost of Student Membership to the SOCIETY was approximately $5.00, and where Student Members were affiliated with Student Chapters, the cost was approximately $6.50.

ACTION: Upon motion, duly seconded, members of the Executive Committee voted to refer this item to the Constitution and By-Laws Committee with request that this Committee provide by-laws for the formation of Vocational Student Membership and Vocational Student Chapters, in addition to the regular Student Membership now in operation; further, that more detailed information was to be prepared on this subject by the Secretary's office and H. E. Rockefeller, such to accompany the referral of this item to the Constitution and By-Laws Committee.

National Program Committee

The Secretary submitted the National Program Committee's recommendation that the names of its subdivision groups be changed in an order signifying their activity, as follows:

(*a*) Annual Meeting Papers Committee to National Papers Committee;

(*b*) Ohio State Welding Conference Papers Committee to AWS Ohio State Welding Conference Committee;

(*c*) West Coast Meeting Papers Committee to West Coast Meeting Committee;

(*d*) Centennial of Engineering 1952 Convocation Papers Committee to Centennial of Engineering 1952 Committee;

(*e*) AWS-AIEE Welding Conference Papers Committee to AWS-AIEE Welding Conference Committee;

(*f*) (Other) Regional Meeting Papers Committee to (Other) Regional Meeting Committee.

ACTION: Upon motion, duly seconded, the Executive Committee approved the foregoing changes.

Committee Appointments

The Secretary submitted recommendations of the National Program Committee that C. G. Herbruck and H. R. Morrison be appointed to the National Program Committee's National Papers Committee.

ACTION: Upon motion, duly seconded, the foregoing recommendations were approved.

Third Board Meeting

The Secretary advised that the Board of Directors orginally planned to hold the third Board meeting in Pittsburgh. Later an invitation developed from the International Acetylene Association to hold the AWS Board meeting at the same time as their Annual Meeting in Indianapolis, which would be on March 31st, April 1st and 2nd, with further invitation that members of the AWS Board join with the IAA in their Annual Meeting activities and with suggestion that the Board members of IAA join for a brief period with the AWS Board members for the purpose of discussion of a joint activity proposal in the southwest in the spring of 1953.

ACTION: The Executive Committee approved the third meeting of the Board being held in Indianapolis on Mar. 31, 1952.

Olean Section

The Secretary presented request for authorization of the Olean Division of the Niagara Frontier Section to change its status to that of the Olean Section of the AMERICAN WELDING SOCIETY, with the possibility of its being named the Olean-Bradford Section. The Secretary advised that the Niagara Frontier Section had granted approval. The action of officially recognizing this Section is nearly one year delayed due to a misunderstanding between the Division and the Niagara Frontier Section. However, this has been clarified but actually the Division's application is based on active membership to 25 members, such application having been submitted prior to by-law change.

ACTION: The Executive Committee voted to accept the application of the Olean Division and authorized that body to organize as a Section of the AMERICAN WELDING SOCIETY.

Weldment Manufacturers Committee

The Secretary reviewed the history of the American Weldment Manufacturers Assn. and the very evident desire of that association to provide within its organization a committee dealing with the various technical requirements for weldments. The dissolution of the AWMA resulted in no further activity in this field and the Secretary is of the opinion that in view of the reputable and very large representation of weldment manufacturers in the original organization of AWMA and their interest in such technical activity, there still remains an unsatisfied desire to provide such service. He feels that our TAC could provide this service and, accordingly, he recommended to the Chairman of the Technical Activities Committee that that Committee consider the organization of such a committee activity. The Secretary sought approval of the Executive Committee of his recommendation to TAC.

ACTION: The Executive Committee approved the Secretary's recommendation to the TAC Chairman, that TAC consider the organization of such an activity.

UNITED STATES
TESTING COMPANY, INC.

INSPECTION and TESTING of
Welding and Weldments
QUALIFICATION of
Procedures and Operators

Main Laboratories **Hoboken, N. J.**

Boston - Chicago - New York - Philadelphia - Providence

WELDING CONNECTORS

Saxe Welding Connection Units position and secure structural parts to be welded.

Clip K3A permits an adjustable connection.

These widely used units eliminate all hole punching, and, with welding, produce the most economical, safe, and quickly erected structural frame.

Write for 1951 edition, Structural Welding Practice Manual.

J. H. WILLIAMS & CO.
Buffalo 7, N. Y.

AIR REDUCTION CANADA, LTD.
Montreal 2, Canada

★ **ARCALOY** for stainless steel

★ **ARMOR-ARC** for armor welding

★ **BRONZE-ARC** for bronze and cast iron

★ **WELD-ARC** for low hydrogen electrodes

★ **NICKEL-ARC** for cast iron

★ **TOOL-ARC** for tool steel

★ **WEAR-ARC** for hard facing

★

NAMES TO REMEMBER IN ARC WELDING

100% of all *Alloy Rods* production is still being sold through our fine Distributor organization. We are shipping more alloy arc welding electrodes than ever before . . . and while our Distributors may temporarily be out of stock on certain types, we urge you to keep in touch with them on your requirements. After all, *quality* is always worth waiting for.

ALLOY RODS COMPANY

No Finer Electrodes Made . . . Anywhere

YORK, PENNSYLVANIA

Alloy Rods Co.

★ *See your nearest Alloy Rods Distributor*

AR-2

National Spring Meeting

The Secretary advised that the membership of the Special Committee to Determine Scope, Purposes and Requirements of the AWS Annual Meeting and he had discussed the advisability of holding a meeting in the southwest in the spring of 1953 in conjunction with other supporting activities. There was no action necessary on this as this was to be developed further with the National Program Committee and to be discussed at the third meeting of the Board of Directors in Indianapolis.

Section Award Certificate

The Secretary advised that as a result of his notifying the Sections that the Board of Directors felt that any program for such awards should be entirely by the Section, and that if they wished there was no reason why they could not issue a certificate for meritorious effort or other activity of value to the Section, he had received a request that the National Society provide a standardized basic certificate for such activity. It was the opinion of the Executive Committee that doing so would be adverse to their original ruling, which was to the effect that this was a matter to be handled wholly by the Sections and that if the Society provided such basic certificate, it was the same as providing national recognition.

Ladies' Auxiliary

The Secretary referred to the Executive Committee the interest of the ladies, wives and family, of AWS members in Detroit in forming a Ladies' Auxiliary. He observed that there were other Sections which had expressed interest including a very strong interest two years ago in Los Angeles. In view of the Detroit interest having developed measurably, he felt that there should be a determination of National Society policy.

Action: Upon motion, duly seconded, members of the Executive Committee instructed the Secretary to advise all Sections that there was a possibility that a Ladies' Auxiliary might materialize, at first in certain Sections as was now evident in the Detroit Section, and that further, such an organization might require national jurisdiction and, accordingly, any provisions that were made for such organization should be made with the understanding that National Society jurisdiction may be considered at a later date.

Adams Lecturer

Dr. S. L. Hoyt, Chairman of the Committee on Awards, 1951–52, submitted a letter dated December 10th (attachment No. 22) in which his Committee proposes that a foreign Lecturer be invited to give the Adams Lecture in 1953, under the following conditions. He should have the usual qualifications, plus a good command of English, and cost of his trip should be paid by some outside source, such as his organization, his government, ECA or other. The Awards Committee recommends that a non-American Lecturer be invited only once in five years. The Executive Committee considered recommendations of the Committee on Awards and decided that there would be no objection on the part of the Executive Committee to a foreign Lecturer provided that he had an excellent command of English, there would be no cost to the Society and that he be internationally recognized. However, they felt that there was an additional proviso required, as follows. In that the award always provides the names of two persons, one an alternate, it is the request of the Executive Committee that the Committee on Awards, whenever a foreign Lecturer is recommended, choose an American as an alternate.

Informational Only

Section Closing: The Secretary officially advised that the Inland Empire (Spokane) AWS Section was closed. There does not appear at this time to be sufficient industrial potential of welding interests capable of supporting a Section activity in Spokane.

Informational Only

District Vice-President: The Secretary advised of the resignation of A. P. Maradudin, District No. 7 Vice-President, due to poor health resulting from a severe operation. He advised that he had acquainted both Mr. Maradudin and the National and Section Officers on the West Coast regarding the necessary arrangements for filling the office of District No. 7 Vice-President.

D-C WELDER SAVINGS PROVED AGAIN IN THE FIELD

Take the case of the 2,000,000-gallon water tank erected by the Pittsburgh-Des Moines Steel Company at Niagara Falls, New York. A battery of D-C Rectifier Welders fed by one large diesel generator replaced a number of diesel-driven welding generators formerly used.

Maintenance was reduced 85%—weight was slashed by 15,000 pounds—initial cost was cut 50%—fuel savings totaled $8.70 a day!

For your welding application—large or small—call your nearest Westinghouse Representative or write Westinghouse Electric Corporation, Welding Division, Buffalo, New York. J-21605

YOU CAN BE SURE... IF IT'S Westinghouse D-C Welders

Buy "PROVEN FLUXES"
Years of GUARANTEED SATISFACTION behind these GOOD
"ANTI-BORAX" FLUXES
Insist on them — Unequalled Quality

No. 1 Cast Iron Welding Flux
No. 2 Brazing Flux for Brass, Bronze, Steel, etc.
No. 4 Braz-Cast Flux for Bronze Welding Cast Iron
No. 5 & 8 Cast & Sheet Aluminum Fluxes
No. 9 Stainless Steel Welding Flux
No. 11 Tinning Compound
No. 16 Silver Solder Paste Flux

Mfg. By
ANTI-BORAX COMPOUND CO., INC.
Fort Wayne, Ind.

NET MONTHLY ADVERTISING RATES
Black and White — Effective October 2, 1950

Space	One Insertion	Three Insertions	Six Insertions	Twelve Insertions
*Full page	$230	$210	$195	$180
Two-thirds page	165	150	140	130
Half Page	130	120	110	100
Quarter Page	80	75	70	65
Eighth Page	50	45	40	35
*Inside Preferred	255	235	220	205

10% Extra for bleed full pages. Color $85 extra per color added

Agency Commission—15%
Cash Discount—2%, 10 days

abstracts of
CURRENT WELDING PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25¢ from the Commissioner of Patents, Washington 10, D. C.

2,549,804—Welding Boot—Harold J. Graham, Highland Park, Mich., assignor to Graham Mfg. Corp., Detroit, Mich., a corporation of Michigan.

Graham's patent relates to a welding boot for use with welding apparatus having a chuck for holding a stud to be welded to a workpiece. The boot is of cylindrical shape and is made from yieldable material. The boot has a circular aperture at one end thereof greater than the diameter of the stud. The boot is carried by the chuck to permit the stud to extend through the aperture in the boot during the welding operations.

2,576,793—Welding Titanium and Other Metal—James Fernando Jordon, Huntington Park, Calif.

This new welding process comprises contacting a preheated metal surface with a gas selected from the group consisting of carbon tetrachloride, hydrogen chloride and mixtures thereof until reaction between the gas and the oxygen-bearing compounds on the heated metal surface has volatilized a substantial portion of said compounds. Thereafter welding is effected under a blanket of such gas.

2,577,123—Method of Welding a Bundle of Aluminum Tubes—Aaron L. Hitchens, New Haven, Conn., and Frederick G. Stroke, Temple, Pa., assignors to Olin Industries, Inc., New Haven, Conn., a corporation of Delaware.

This welding method comprises preheating a bundle of aluminum tubes to less than welding temperature, applying heat to a welding member having a plane horizontal welding surface to raise it to welding temperature, and covering the welding surface with flux. The tube ends are brought into engagement with the welding surface to melt the ends without melting the tube bodies and the tube bundle and welding member are separated immediately after the desired portion of the tube ends have been melted so that the resulting molten metal autogenously casts the ends of the tubes together.

2,577,124—Bonding Unhexed Tubes—Martin Newcomer, Cheshire, Conn., assignor to Olin Industries, Inc., New Haven, Conn., a corporation of Delaware.

This patent covers apparatus for performing a method very similar to that covered in Patent 2,577,123. However, the welding member has a generally flat and horizontal surface with a plurality of relatively flat pyramidal studs extending therefrom. The tube ends are brought into engagement with the studs to be expanded, shaped to contour of the stud base, and melted simultaneously so that the thus shaped tube ends will be secured together by the resultant molten metal.

2,577,163—Resistance Welding and Resistance Weld Testing—Eugene R. Spittler and Ivar W. Johnson, Schenectady, N.Y., assignors to General Electric Co., a corporation of New York.

In this patent, a resistance-welding method is covered wherein the voltage across a weld is measured during its formation. The welding current flow is interrupted when the voltage across the weld attains a substantially constant value.

2,577,258 — Alternating - Current Welder—Francis B. Miller, Dayton, Ohio, assignor to The Commonwealth Engineering Company of Ohio, Dayton, Ohio, a corporation of Ohio.

Miller's patent relates to a welding transformer wherein a single closed magnetic circuit links a primary and a secondary coil. A three-phase power supply system has one phase connected to the primary coil, and first and second auxiliary coils are positioned to be linked by the magnetic circuit. First and second filters connect respectively to supply the first and second auxiliary coils and they are connected to the second and third phases of the power supply circuit.

2,577,411—Sequence and Welding Timer—Donald P. Faulk, Lexington, Mass., assignor to Raytheon Mfg. Co., Newton, Mass., a corporation of Delaware.

This patent is on a timing system for use in supplying current through a load from a power source. The system includes a pair of discharge devices and means for causing current to flow in one discharge device. Time control means are required and operate only when one discharge device is conductive for causing the other discharge device to conduct current through the load. Means are provided to extinguish the first of the discharge devices and thereby extinguish the other discharge device.

2,577,607—Air Supply System for Welders' Helmets—Thomas A. Conley, Harrisville, R. I., assignor to American Optical Co., Southbridge, Mass., a voluntary association of Massachusetts.

This welder's helmet has an oval-shaped member pivotally positioned between supports. An air line extends into the oval-shaped member and filtering material overlies opposed open sides of the member to form a chamber into which air is directed by the air line.

2,578,065—Apparatus for Flame-Hardening Rounds—Howard G. Hughey, Fanwood, N. J., assignor to Air Reduction Co., Inc., New York, N. Y., a corporation of New York.

This apparatus includes a base member at least partially surrounding a cylindrical metal body in spaced relation thereto. A series of torch tip supporting arms are swiveled to the base member at a number of points spaced circumferentially of the metal body and project inwardly toward the metal body tangentially thereof. A liquid-cooled torch tip is carried by each tip supporting arm so that regardless of the angle of the arms to the metal body, the heating flames from the tips can be directed substantially normal to the surface of the cylindrical workpiece.

2,578,074—Welding Machine — Henry Kershaw, Belleville, N. J. assignor of one-sixth to Herbert L. Zuckerman, one-sixth to Arthur E. Zuckerman and one-sixth to Honi Zuckerman, all of Maplewood, N. J.

This welding machine includes a support and a substantially vertical electrode mounted thereon and provided at its upper end with a recess for receiving a single ball. Two movable carriages are provided in the apparatus for movement in vertical and horizontal directions and a pen point holding block is secured to one carriage. Means are provided for electrically connecting the welding circuit with the electrode and the pen point holding block.

2,578,132—Feed Control Attachment for Torches—John Roy Garrett, Aurora, Ill.

This patent relates to a cutting torch guide device wherein downwardly extending magnets are carried by a frame member for engaging an attracting surface. A flat elongate guide is provided and frames are included in the apparatus and are longitudinally adjustable on the guide member.

2,578,171—Combination Welder's Face Shield and Helmet—Robert A. Bub, Fittsburgh, Pa., assignor to Mine Safety Appliances Co., Pittsburgh, Pa., a corporation of Pennsylvania.

an ACCO product

Stainless Steel WELDING RODS

AC–
DC–
or GAS
made by
PAGE

BURNING...
stable even at lower heats

SLAG...
clean, easily removed

COATING...
resists cracking down to very short stubs

SELECTION...
complete line for welding
every type of stainless

Get in touch with your PAGE distributor

ACCO

PAGE STEEL AND WIRE DIVISION
AMERICAN CHAIN & CABLE

Monessen, Pa., Atlanta, Chicago, Denver, Detroit, Los Angeles, New York, Philadelphia, Portland, San Francisco, Bridgeport, Conn.

PAGE WELDING RODS

This patent covers a specialized connection of a welder's face shield to an industrial helmet.

2,578,832—Can Body Welding Machine Including Movable and Rotatable Electrode—Paul E. Pearson, New York, N. Y., and Alfred Treff, Chicago, Ill., assignors to Continental Can Co., Inc., New York, N. Y., a corporation of New York.

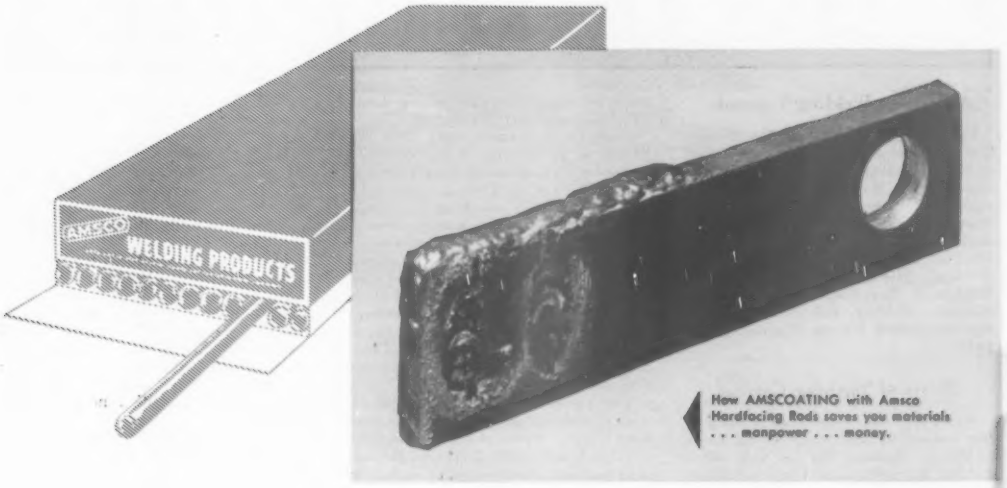
The patented can body welding machine includes a welding horn and a stationary electrode extending longitudinally of the horn. A carriage is mounted for longitudinal movement relative to the horn while an electrode is rotatably mounted on a cross-head reciprocably carried by the apparatus. The electrode is moved with relation to the carriage as the carriage is reciprocated.

2,578,835—Welding Unit—Albert F. Pityo, Clifton, and Harry E. Butterfield, Jr., Maplewood, N. J.

In this welding device, electrode holding means are provided as are means to move at least one of such electrode holders with relation to the other. Such means include first and second opposed arms with an elongate leaf spring arranged between them. Adjustable means are furnished to vary the tension of a free end portion of the leaf spring that engages one of the arms.

2,579,097—Apparatus for Brazing—Frank W. Schwinn, Chicago, Ill.

This patent relates to an elongated and strengthened brazed joint that connects a plurality of noncomplementarily shaped members. A steel clip conforms to and embraces opposed side portions of intermediate members in the joint and it also has surfaces conforming to and engaging the side members of the joint. Brazing material is secured between both the side members and the intermediate members and the conforming portions of the clip.



How to make short work of a long shutdown!

AMSCOATING...stands for control of wear by Hardfacing...

Hardfacing rods—and recommendations for their use—are as sound as the manufacturer who makes them. AMSCO has been fighting wear for a half-century—first with Manganese Steel, and later with AMSCO Hardfacing Products.

If you have a problem of wear caused by impact, abrasion, heat or corrosion . . .

Find out how AMSCOATING can save you materials . . . manpower . . . money!

This AMSCOATED Pulverizer Hammer has cut replacements over 90% to date!

Big savings are bound to result when you can reduce shutdowns and replacements. For example, coal pulverizer hammers in a Southern plant were lasting about 5 days before a complicated dismantling job for replacement was necessary. Some hardfacing methods had been tried, but the results still weren't good enough to bring costs down to earth.

Recently test hammers were AMSCOATED with an Amsco Rod specially developed for this type of service . . . and the hammer you see above is now making service life history! Why?

The plant superintendent reports that the AMSCOATED hammers are lasting 14 times longer than plain hammers . . . over twice as long as hammers hardfaced by any other method tested . . . that they've been in service 72 days and are still going strong!

This actual example is one of many AMSCOATING applications that are resulting in *lower* maintenance and replacement costs. . . . *higher* production. Write today for catalog on dollar-saving AMSCO rods and the name of your nearest AMSCO distributor.

AMSCOATING

THE RIGHT WAY TO SAY HARDFACING

AMERICAN Brake Shoe COMPANY

AMERICAN MANGANESE STEEL DIVISION

399 EAST 14th STREET • CHICAGO HEIGHTS, ILL.

Other Plants: New Castle, Del., Denver, Oakland, Cal., Los Angeles, St. Louis. In Canada: Joliette Steel Division, Joliette, Que.
Amsco Welding Products distributed in Canada by Canadian Liquid Air Co., Ltd.

NEWS OF THE INDUSTRY

American Welding Expands

Further expansion here in the production facilities of The American Welding & Manufacturing Co. were made known recently with the announcement of the signing of an agreement with The Warren Machine & Die Co., whereby the latter, effective January 1st, became the Warren Machine & Die Division of American Welding. This was made public jointly by William J. Sampson, Jr., president of American Welding, and John W. Slavik who established Warren Machine in late 1946.

Tests of Turbine Casing

Strain gage tests were made recently on the welded turbine scroll casing for a 70,000-hp. turbine, first of two which Baldwin-Lima-Hamilton Corp. is building for the Detroit (Oregon) Dam power plant of the Corps of Engineers, U. S. Army. SR-4 resistance wire strain gages and indicator, products of the Testing Equipment Dept. of Baldwin, were used in tests supervised by R. B. Willi, manager of the Hydraulic Turbine Engineering Division.

The 260,000-lb. turbine scroll casing is made of seven welded sections of heavy steel plate with flanges for bolting the sections together. Six of these sections are shown in Fig. 2 which indicates the size of the casing. Maximum diameter of the spiral is 40 ft. and the opening at the large end (not shown) is 150 in.

In preparation for the tests SR-4 strain gages were bonded by standard procedure at 17 critical points including welds, areas between welds, in fillets, and on the three-point intersection of the bolting flange, rolled plate and stay ring casting.

Since these strength tests were performed in conjunction with hydrostatic leakage tests, moisture proofing was necessary. The first step was an application of cerese wax. An area around each gage was then covered with a waterproof paper which was in turn sealed to the casing by means of waterproof, 2-in. wide surgical adhesive tape. The whole was then painted over with an alkyd resin paint.

In order to fill the casing with water, it was sealed by means of a 150-in. diameter drum head on the intake end and by a barrel ring on the inside bore of the stay ring casting.

Test pressures of 95, 190 and 285 psi. were applied by pump and shop accumulator system. Seven strain measurements were made with each gage at each pressure. Zero readings were repeated on each measurement and found to return to within plus or minus 5 micro inches.

Features of the shop-welded casing, the first of large size to be built in Baldwin shops, are its lower weight and cost as compared with an equivalent casing design in cast steel. Weight saving amounts to 20 to 30% and gives 15 to 20% saving in cost while insuring against defective material, Baldwin engineers report.

British Industries Fair

The 1952 British Industries Fair will be held in London and Birmingham, England, May 5th to May 16th, according to announcement in London.

Sir Frank Lee, permanent head of the Board of Trade, reported Britain's National Trade Show in 1951 as attracting "the record number of 19,266 overseas visitors," and declared that plans were being made to increase this high figure for the 1952 exhibition. This is the largest overseas attendance in the 36-year history of the Fair.

The 1952 Fair will again occupy three exhibit halls, Earl's Court and Olympia in London, and Castle Bromwich in Birmingham. Every square foot of exhibit space in the three buildings was utilized last year by nearly three thousand British manufacturer exhibitors, with a large outdoor area in Birmingham being allocated to road construction and other heavy machinery.

Special train service between London and Birmingham will be provided as in previous years.

Welding in Ship Construction

The International Institute of Welding will hold its next meeting in Gothenburg, Switzerland, September 7th to 14th. At this particular meeting Welding in Ship Construction is to be featured. Members of the AMERICAN WELDING SOCIETY (as are members of the other Societies forming part of the International Institute of Welding) are requested to submit draft papers on this subject to the Scientific and Technical Secretariat before Mar. 1, 1952. If any member of the SOCIETY wishes to submit a paper, they are urged to send in draft to the Scientific and Technical

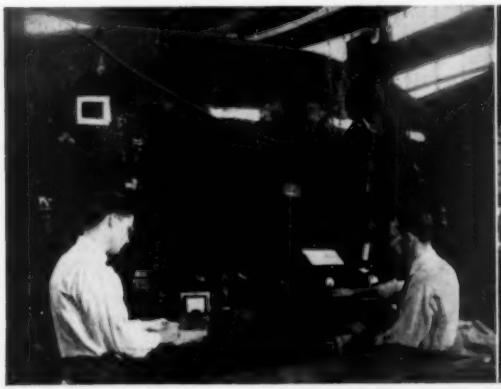


Fig. 1 Baldwin-Lima-Hamilton Corp. turbine engineers used the corporation's own SR-4 strain gage testing equipment to measure strains in the 1¹/₄-in. walls of a welded, 260,000-lb. turbine scroll casing while under hydraulic pressure. Strain deflections of 17 gages bonded at critical-points were determined by means of the latest Type L portable SR-4 strain indicator with two 20-point switch boxes

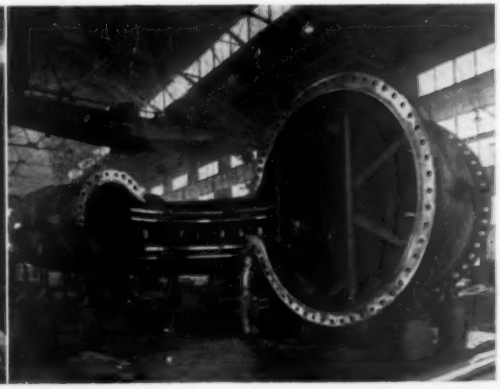


Fig. 2 The first large turbine scroll casing to be fabricated by welding by Baldwin-Lima-Hamilton Corp. consists of seven sections held together by bolted flanges. Six sections are shown. Inside diameter at the large end is 138³/₄ in. Walls are 1¹/₄ in. thick. Maximum diameter of the ring is 40 ft. The casing was built for a 70,000-hp. turbine in the Detroit (Oregon) Dam power plant

Take advantage of exclusive P&H AC Dial-lectric principle

give your production a lift

— with safe, open-circuit voltage . . . easy, quick-start arc . . . time-saving, built-in remote control . . . compact AC design

Armor plate welding calls for low-hydrogen or stainless steel electrodes. P&H, pioneer in the low-hydrogen field, also offers the AC welder that gives you the best weldability with these rods — for greater production.

Your men like the P&H AC Dial-lectric — like its safety under the most adverse conditions; like the easy, quick-start arc, the elimination of arc-blow. And they get more done! Get sounder, better looking welds in less time, thanks to built-in remote control which lets them select the right heat at the work, turns *walking time* into *welding time*.

Because these welders give production such a lift, P&H has stepped up its delivery schedules to meet defense demands without delay. Get your production rolling in high gear. Order your P&H AC Welders today. Capacities up to 650 amps. Take advantage of this delivery situation while it lasts.

P&H **WELDING DIVISION**
4551 W. National • Milwaukee 14, Wis.
HARNISCHFEGER CORPORATION

Secretariat, 32, Boulevard de la Chapelle, Paris 18, France. They should be sent in three copies since they will then be examined by the President, the Treasurer and the Scientific and Technical Secretary who will choose from among the papers received those to be presented during the special session. After the Gothenburg assembly the Bureau hopes that it will be possible to have the papers submitted, including those not presented, published in national welding reviews.

1952 ASTM Lecturers Announced

Two significant lectures are in prospect for the Fiftieth Anniversary Meeting of the American Society for Testing Materials to be held in New York City during the week of June 23, 1952.

Dr. R. C. McMaster, Battelle Memorial Institute, will deliver the 26th Edgar Marburg Lecture on the subject "Nondestructive Testing." In selecting this subject for the lecture, the ASTM directors were conscious of the ever-growing interest on methods of evaluating materials by nondestructive means and Dr. McMaster was a logical choice to receive the invitation because of his outstanding work in the field, his close connection with ASTM Committee E-7 on Non-Destructive Testing, and the recognition accord him as a forceful speaker and writer. His 1950, 80-page ASTM technical paper, "A Basic Guide for Management's Choice of Non-Destructive Tests," was outstanding.

The first H. W. Gillett Memorial Lecture—the establishment of which was recently announced—is to be given by N. L. Mochel, Manager, Metallurgical Engineering, Westinghouse Electric Corp. Mr. Mochel has selected the subject "Man, Metals and Power." He is expected to include, as a portion of his lecture, the background of some of the outstanding work done by Dr. Gillett, and in keeping with Gillett's broad interest—that is, the critical evaluation of metals and alloys—the lecture will devote a major portion of his address to the use of materials in the field of power generation, including high temperature and related applications.

The Gillett and the Marburg lectures will be presented on Monday and Wednesday afternoons, respectively, June 23rd and 25th.

Fabricating Low-Pressure Turbine Cylinder

Undergoing fabrication in Allis-Chalmers tank and plate shop is this center section of the low-pressure cylinder for the first of two 120,000-kw., 3600/1800-rpm. cross-compound steam turbine generator units for Wisconsin Electric Power Co.'s new $100,000,000 Oak Creek power plant under construction in the southeast corner of Milwaukee County.

The center blade-retaining ring being welded to the plate steel center section is of cast steel. The operation is conducted on a 50-ton motorized positioning table which can be adjusted from the horizontal to a 90-degree angle to rotate a piece nearly 27 ft. in diameter at from 8 in. to 20 ft. per minute table rim speed. The table, largest of its kind at Allis-Chalmers, greatly facilitates welding of large pieces. The 50-ton capacity denotes this weight with mass center 3 ft. from the table when in the vertical position.

TWECO TERMINAL CONNECTORS

for Welding Machines

Bolt directly to positive and negative welder studs. Provide easy cable switch to reverse polarity. Simple cable disconnect for quick "Jump-In" of additional cable.

3 Sizes for cables #6 through 4/0

The male plug of TWECO "Sol-Con" and "Mac-Con" Cable Connectors connect with TWECO Terminal Connectors. Sol-Con and Mac-Con Cable Connectors are COMPANION ITEMS.

Write for Twecolog #8 giving data and prices on the complete TWECO line of electrode holders, ground clamps and cable connections for electric welding.

See Your Welding Supply Distributor

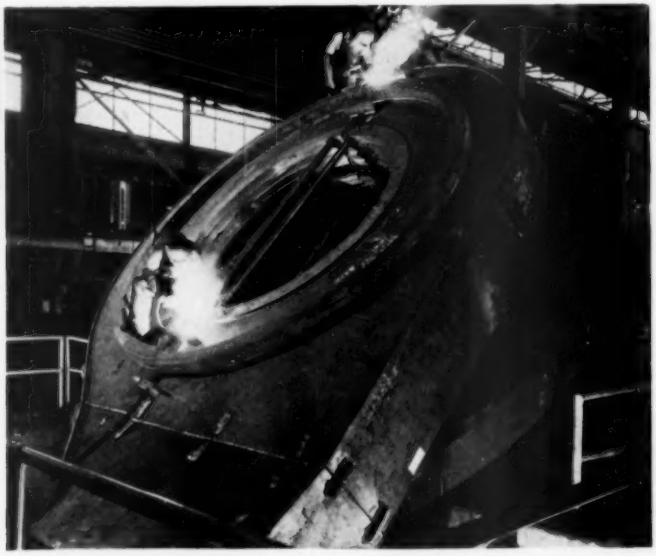
Tweco PRODUCTS COMPANY

TWECO PRODUCTS

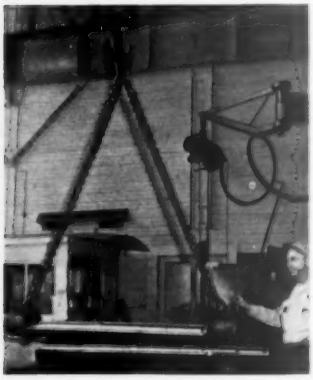
BOSTON at MOSLEY
WICHITA, KANSAS

Antifumbling Device

They don't drop shafts at the Torrance (California) plant of The National Supply Co., manufacturers and distributors of oil field machinery and equipment. A short piece of pipe, capped on one end and with welded hook and handle, is placed over each end of a shaft prior to lifting. The open end of the pipe is flared to make positioning easier.



Fabricating Low-Pressure Turbine Cylinder



Antifumbling Device

ANOTHER EXAMPLE OF

DESIGN LEADERSHIP*

SQUARE D's NEW 4-way VALVE for SPOT WELDERS

ROTARY MOTION

FOUR-WAY
⅜" PIPE

Fast Operation—Direct-acting magnet...short, rotary motion . . . will operate up to 600 times per minute.

Fast Air Movement—Internal port areas greater than ⅜" pipe area . . . minimum resistance to flow . . . designed to get maximum response speeds from welding guns or stationary machines.

Long Life—Pressure-balanced rotor . . . hardened magnet parts . . . oilite pivot bearings . . . spring loaded vanes to provide positive air seal after millions of operations.

Low Power Consumption—Magnet coil requires only 38 V.A. at 60 cycles . . . inrush 215 V.A.

Compact—Overall size only 5¾" x 6½" x 4"

Accessible—Valve body and magnet structure removable without disturbing pipe connections . . . magnet coil easy to change.

Easy Installation—Mounts in any position . . . cast base with 3-point mounting reduces possibility of distortion.

Write for Bulletin 9043 Type C, Square D Company, 4041 N. Richards Street, Milwaukee 12, Wisconsin

SQUARE D COMPANY

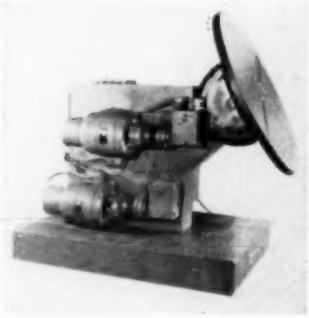
DETROIT • MILWAUKEE • LOS ANGELES

SQUARE D COMPANY CANADA LTD., TORONTO • SQUARE D de MEXICO, S. A., MEXICO CITY, D.F.

NEW PRODUCTS

Automatic Welding Positioner

In the new Model "21" Precision Automatic Welding Positioner the table rotates at any infinitely variable speed from 0 to 2.7 rmp. and at 4 rpm. constant speed for quick positioning. Either variable or constant speed is immediately available on a remote push-button control station. Precise table speeds are indicated on an electric generator tachometer. Speeds can be read as close as 0.025 rpm. with no fluctuation of the knife-edge needle.



The table tilt is accomplished through a 3-hp. brake motor affording instant stopping. The table tilts 135 degrees in 23.4 sec. Precision limit switches control the degree where the table will stop either "Tilt" or "Flat."

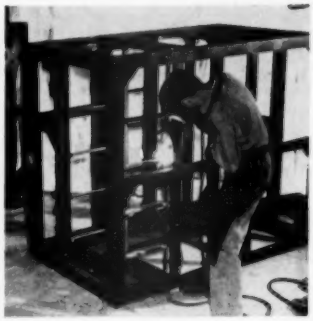
The entire body of the "Positioner" raises 24 in. This is accomplished through a 3-hp. brake motor. Guide rollers elevate on two columns through a rack and pinion. Precision limit switches control desired height both top and bottom between 24 in.

The entire Positioner is mounted on 8-in. grooved casters that raise the base only 2 in. off the floor. The "V" groove allows making a track for the Positioner to be guided from station to station if desired.

New Tin Plate Thickness Gage

Benjamin Korwek, Research & Control Instruments Division, North American Philips Co., Inc , Mount Vernon, N. Y., welds metal members into position on iron frames for large X-ray units used by industry to gage thickness of tin plating on steel sheets. Members being welded into the frame are slides on which electronic chassis units will move in and out for convenience in servicing.

These new machines, announced for the first time a year ago, direct an X-ray beam at an acute angle onto the plate to be gaged. This causes the steel in the plate to give off secondary fluorescent radiation



and intensity is measured after passing through the tin coating. A Geiger counter connected to electronic circuits and a tape recorder make it possible to automatically translate electrical impulses directly into thickness units.

Improving Nozzle Tips

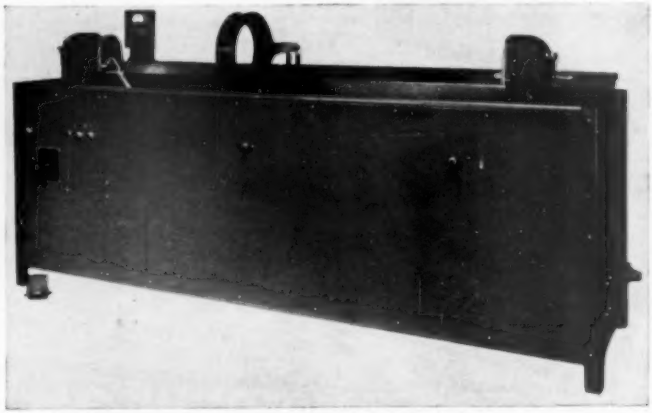
By adding a Carboloy cemented carbide bushing inside the conventional feed nozzle tip for automatic arc welding, the General Electric Co. at their Fort Wayne Works has increased life of the nozzle tips up to 20 times. Wear is no longer a problem, and burning effect greatly reduced, it is claimed.

In one case where the coiled electrode wire is especially abrasive, brass nozzles had to be replaced six to eight times per week. The carbide protected nozzles are giving a service life of three months and better. On other operations where former life had not exceeded three weeks, the carbide nozzles are lasting as long as 12 months. In addition to the improved nozzle life, a more uniform narrow weld can now be maintained, as accurate guidance of the wire—formerly difficult once wear developed—is no longer a problem. In view of these several benefits, the use of carbide-equipped nozzle tips has been made standard on about 20 automatic arc-welding machines.

Magnaflux Redesign Reduces Cost

A complete re-engineering by Magnaflux Corp., 5900 Northwest Highway, Chicago 31, Ill., is announced, creating a full new line of equipment for magnetic particle inspection. This has greatly improved delivery time and, in many instances, reduced costs to meet the expanding needs for Non-Destructive Testing created by the needs of the national defense program. This was done while "special" inspection equipment requirements were making excessive demands upon the company's engineering facilities during 1951.

A number of detail improvements were made on the new line of standard units. In most cases the new units are larger than the old units at comparable price—from 48 in. old to 54 in. new capacity, and from 72 in. old to 96 in. new. The new units can handle a greater diversity of parts for inspection. They are now built for either 220 or 440 v., 60 cycles, interchangeably. Maintenance, though a relatively minor item, has been reduced and/or simplified.



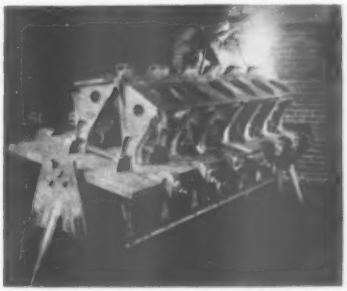
New type Magnaflux unit, the direct current type ARQ, with 96 in. capacity and 6000 amp. magnetizing current output. Note integral welded frame and tank design, with only two front panels attached as separate items. Folding Magnaglo hoods are available for all new type units (not shown here)

BEST FOR 3 OUT OF 4 WELDING JOBS!

WELDING SPEEDS INCREASED 20% after this company switched to AC. Deposition rates may be doubled because AC permits the use of higher currents and larger electrodes.

AC ELIMINATES TROUBLESOME ARC BLOW, permitting these weldors to deposit $\frac{3}{8}$ inch fillet up to 20% faster. AC is the ideal choice for deep fillet welding because the operator can easily control the arc.

AC CUT COSTS 10% and increased welding speeds 15% for this iron works. With AC, you save four ways —on labor, electrodes, power and original cost.



IMPROVED WELD QUALITY enabled this plant to meet rigid specifications on diesel engine crank cases and marine gear cases. The absence of arc blow makes it easy to deposit weld metal smoothly and uniformly, even in grooves and corners.

G-E AC Welders Increase Speed, Improve Weld Quality, Cut Costs, Eliminate Arc Blow

See Your G-E Arc Welding Distributor for complete information on G-E AC welders. They are available in 200- to 500-amp ratings for indoor or outdoor use. Most models are available on short delivery. Your distributor also carries a complete line of DC, engine-driven, Inert-Arc and Atomic-Hydrogen welders, plus electrodes and accessories. You can find your nearest distributor's name by looking for General Electric under "Welding Equipment" in the yellow pages of your telephone directory.

Section C711-20, General Electric Company, Schenectady 5, N. Y.

Please send me immediately publications on the following G- AC welders.

- X for immediate project
- √ for reference

- ☐ 200-amp, GEC-553
- ☐ 300-amp, GEC-652
- ☐ 400-amp, GEC-750
- ☐ 500-amp, GEC-812
- ☐ 300-amp Indoor or Outdoor Model, GEC-779

Name .....

Company .....

Address .....

City ..... State .....

GENERAL ELECTRIC

SECTION NEWS AND EVENTS

as relayed to C. M. O'Leary

Meetings and Lectures

Baltimore, Md.—The schedule of meetings of the *Maryland* Section for the balance of the year is as follows:

Feb. 15—"Handling Difficult Welding Problems" by A. G. Hogaboom, Bethlehem Steel Co.
Mar. 21—"Welding Air-Hardening Alloy Steels" by W. H. Wooding, Philadelphia Naval Shipyard
Apr. 18—"Welded Fabrication for High-Temperature Applications" by R. W. Clark, General Electric Co.

The Maryland Section is sponsoring a series of Educational Lectures at Johns Hopkins University starting January 16th. This series is designed to prepare supervisors, engineers, designers and draftsmen to handle welding problems in the fastest and most economical manner. Six integrated lectures of 1½ hr. duration with question period will be given. The lectures are designed to provide a better understanding of the generally applied welding processes. Membership in AWS is not necessary for attendance. The balance of the lectures are as follows: Feb. 13—Machinery, Pressure Vessels and Pipe Welding; Feb. 20—Trouble Shooting.

Metal Spraying

Boston, Mass.—At the December 10th meeting of the *Boston* Section held in the Graduate House, M.I.T., Keith Madison substituted for Vernon Cook, Vice-President of Metallizing Company of America, and gave a very good talk on Metal Spraying covering a wide range of applications. A very interesting discussion followed with 12 of the 52 persons present participating.

The speaker at the dinner, which was held in the Campus Room, was Oscar Tenenbaum, Chief Meteorologist in charge of Weather Bureau at Logan Air Port in Boston. His subject "New England Weather" was made very interesting and instructive.

Plant Visit

Buffalo, N. Y.—The February meeting of the *Niagara Frontier* Section will be held on Thursday, February 28th, and will be in the nature of a Plant Visit of the Strip Mill of Bethlehem Steel Co., from 1:30 to 5:30 P.M. Dinner will be served at 6:30 P.M. at the Executive Club of the Company. The meeting is called for 8 P.M. R. E. Somers of the Research Department, Bethlehem Steel Co., will be the speaker.

Resistance Welding

Cleveland, Ohio—J. H. Cooper, AWS, Chief Sales Engineer of Taylor-Winfield, Warren, Ohio, gave a well-rounded talk to 88 *Cleveland* Section members and guests on "Unusual Applications of Resistance Welding" in the Hotel Allerton's Mather Room at the Cleveland Section's third regular meeting on December 12th. Mr. Cooper covered a broad subject in a short time, supporting his presentation by better than 60 slides, portraying resistance welding as a most versatile tool in the art of joining metals. A large number of the illustrations were of unusual applications. He presented the metallurgical, engineering, design, inspection and testing aspects of the process and also dealt with quality control and economics. His talk was followed by questions and discussion on the subject. Mr. Cooper joined the Taylor-Winfield Corp. in 1937, when he served in various capacities including the functions of testing, development and application engineer. He holds B.S. and M.S. degrees in electrical engineering. He was introduced by Bob Henry, Technical Chairman.

Following the opening dinner, Ross Yarrow, Coffee Chairman introduced W. D. Martin of the Republic Steel Corp., who, under the title, "Red Gold from Land of Sky Blue Waters," gave a very interesting travelogue of the running and transportation of Minnesota and Wisconsin iron ore to Lake Erie ports, all illustrated with colored slides and a Land of Hiawatha prologue.

An old friend, who described himself as a Cleveland Section member adopted by Detroit, attended the activities. He was T. J. (Tom) Crawford, AWS District Vice-President. He urged that all Cleveland Section members, whenever they are in the neighborhood of other AWS Sections, when and where meetings were being held, to attend those meetings and to announce their identity. This would not only broaden the individual's welding know-how and field of acquaintance, but would assist Section meeting attendance and particularly encourage smaller Sections. Further, all Sections, in their reports to THE WELDING JOURNAL, should record the attendance of out-of-Section visitors. A cordial invitation is extended by the Cleveland Section to visitors from all other Sections to attend its meetings, make their presence known, and be introduced all around. Section Chairman John Austin announced that the December 5th Cadillac Tank Plant Inspection Trip was a real success, as over 100 from Cleveland and neighboring areas of Akron and Lorain made the tour, which was followed by dinner in the plant and talks by several of the plant officials.

All Cleveland Section meetings will be held at the Allerton Hotel, the 2nd Wednesday of each month, except May, in which it will be held on the 2nd Friday. Visiting members will be heartily welcomed and should plan to arrive about 6 P.M. so as to get acquainted with everyone.

Resistance Welding

Dallas, Tex.—Al Mellor, AWS, Welding Engineer, Chance Vought Aircraft, Dallas, Tex., and Ben Myers, gave interesting talks of general interest on the subjects, "Spot Welding and Quality Control" and "Heavy Steel Spot Welding," at the December 12th meeting of the *Dallas* Section held in the Recreation Room of the Mosher Steel Co. A film "This Is Resistance Welding" was shown in conjunction with the talks and also was received with interest. Refreshments were served after the technical session.

Resistance Welding

Dayton, Ohio—J. H. Cooper, AWS, Chief Sales Engineer of the Taylor-Winfield Corp., Warren, Ohio, touched on the various processes of spot, projection, seam and flash butt welding of hardenable steels, in his technical talk given before the *Dayton* Section on December 11th. Mr. Cooper is well qualified to discuss this subject having spent nearly twenty-three years in the electrical welding field since receiving his degree in Electrical Engineering from the University of Missouri in 1929.

An excellent color sound film highlighting the fabrication and erection of the steel for the United Nations Bldg. was shown before the technical talk.

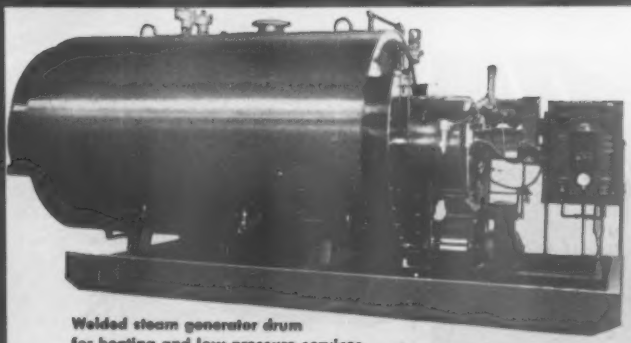
An informal dinner meeting at the central Y.M.C.A. cafeteria was enjoyed prior to the technical meeting.

Flame Spraying

Denver, Colo.—W. D. Hooper, AWS, Linde Air Products Co., Division of Union Carbide and Carbon Corp., Denver, covered new applications and processes in his talk on "Polyethylene Flame Spraying" given at the December 11th dinner meeting of the *Colorado* Section held at the Oxford Hotel, this city. A general talk on the weather, climate and the U. S. Weather Bureau was given by the coffee speaker, A. W. Cook of the U. S. Weather Bureau.

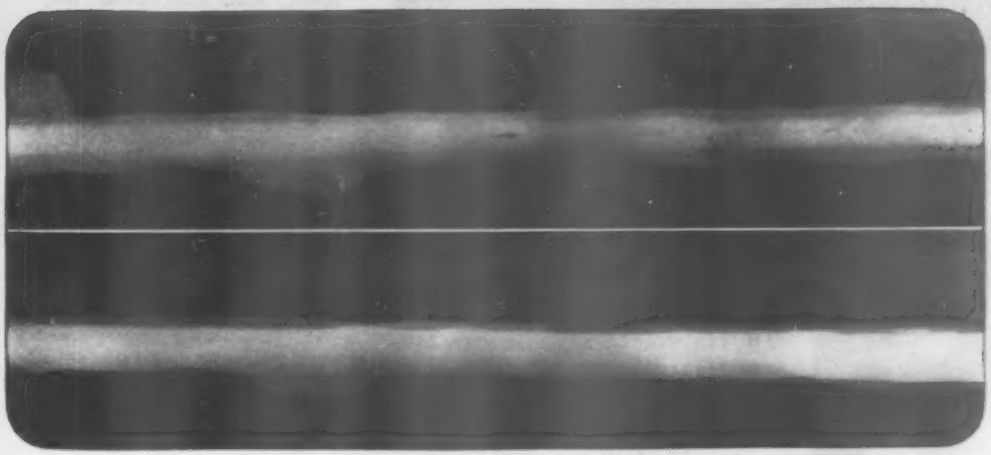
Electric Welding Meeting

Detroit, Mich.—An Electric Welding Meeting sponsored by the AIEE was held in the Detroit Edison Auditorium on December 6th. Two interesting papers were given on the subject "Instrumentation for the Service and Setup of Resistance Welding Machines" by I. W. Johnson, AWS, Welding Section, Schenectady



Welded steam generator drum
for heating and low-pressure services.

Dependability up—cost and weight down—



thanks to RADIOGRAPHY

BECAUSE radiography proves it sound, welding can be used for making better pressure vessels. And with results such as these: In these steam boilers approximately 15% in weight was saved—costs dropped 10% to 20% dependent on design pressure.

Easy to see, isn't it, how radiography opens new fields for welding—why it increases business.

Radiography is quick to detect any serious lack of fusion or gas porosities in a weld. It can help you make sure only top-quality work leaves your shop.

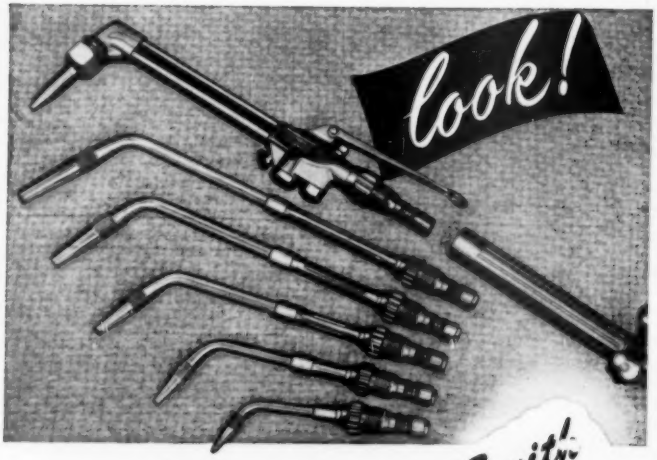
If you would like to know more about how it can help you, get in touch with your x-ray dealer and talk it over.

EASTMAN KODAK COMPANY
X-ray Division • Rochester 4, N. Y.

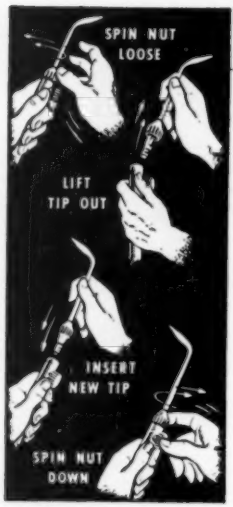
Radiography . . .

another important function of photography

Kodak
TRADE-MARK



You can change these Smith Tips in 6 Seconds



- No wrench necessary
- Fingers do the job
- Fast line-up of tip
- Fast change from welding to cutting

How do you like that for fast, efficient operation? No searching for wrenches—no wasted time. Just use fingers only. In actual demonstrations, operators have been able to remove and replace 4 tips in the time another operator could remove and replace 1 tip in an old fashioned model. And the seal stays tight even if tip is battered or nicked!

This is the kind of progress modern industry needs and wants. But it's not the only advantage you get with this new Smith equipment. Note that the Cutting Assembly can be attached just as quickly as a tip and the new "FLO-TROL" feature eliminates reverse flow of gas—prevents burned out seats. Keeps torch on the job . . . does away with costly delays. Another ingenious feature permits rotating the tip **while flame is burning!** (Heavy Duty Torch Body and Cutting Assembly, illustrated above, carries Lifelong Guarantee).

THAT'S ALL! . . . JUST 6 SECONDS; IT'S NEW AND REVOLUTIONARY

MAIL COUPON TODAY FOR DETAILS

SMITH WELDING EQUIPMENT CORPORATION

Manufacturers of fine Welding Equipment for over 40 Years

Smith Welding Equipment Corporation
2634 S.E. 4th, Minneapolis, Minn. Dept. WJ-1

Please send me more information on the time-saving and money-saving features of your new torch.

Name_____

Address_____

City_____ State_____

Works Laboratory, General Electric Co., and J. J. Riley, AWS, Chief Electrical Engineer, Taylor-Winfield Corp., Warren, Ohio.

Regular monthly meetings of the *Detroit* Section for the last three months of the season are as follows: Mar. 14—Resistance Welding; Apr. 18—Third Welding Conference; May 17—Ladies Night Party. A definite interest has been expressed by the ladies for forming a Ladies' Auxiliary. The Ladies' Auxiliary Committee is in charge of J. R. Stitt, of the R. C. Mahon Co., P. O. Box 4666, Detroit 34.

Annual Company Night

Detroit, Mich.—Two hundred and fifty-five members and guests of the *Detroit* Section ventured out in spite of the first heavy snowstorm of the year on Friday, Dec. 14, 1951, to attend the annual Company Night meeting sponsored by the Weltronic Co. They were rewarded by a most interesting and enjoyable meeting.

Latest designs in Resistance Welding Controls with actual working models were available for inspection. Standard non-synchronous and special synchronous precision controls were available as well as three-phase frequency converter controls for spot and seam welding.

A film "Ground Control Approach" was shown through the courtesy of the Air Pictorial Div., Washington, D. C. This film described the GCA method of landing aircraft distressed by weather or other hazardous condition.

Entertainment was made available through arrangements with Chuck Stanley of WXYZ-TV.

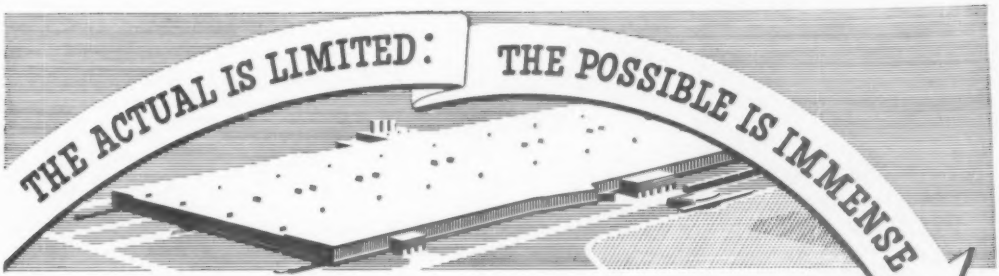
Refreshments were enjoyed after the meeting.

Welding Conference

Detroit, Mich.—The third annual Welding Conference, sponsored by three leading technical societies, has been scheduled for April 16th, 17th and 18th at the Rackham Memorial Bldg., this city. A full program of papers and exhibits, together with a plant trip, is being arranged jointly by the American Institute of Electrical Engineers, the AWS *Detroit* Section, and the Industrial Electrical Engineers Society of Detroit.

C. M. Clark of the Duquesne Light Co., Pittsburgh, is the chairman of the 1952 meeting. His executive committee consists of the following:

Papers—E. J. Limpel, A. O. Smith Corp., Milwaukee, Wis. Publication—C. M. Rhoades, Jr., AWS, General Electric Co., Schenectady, N. Y. Publicity—J. S. Francis, Consumers Power Co., Jackson, Mich., and Stanley H. Brams, AWS, McGraw-Hill Publishing Co., Detroit. Demonstrations—Dr. A. DiGiulio, AWS, consultant, Detroit. Conference Treasurer, G. W. Garman, AWS, General Electric Co., Detroit. Local Chairman—S. W. Luther, Detroit Edison Co., Detroit. Secretary—J. F. Deffenbaugh, AWS, Federal Machine & Welder Co., Warren, Ohio. AWS Representative—Keith Sheren, Swift Electric Welding Machine



NEW LINCOLN PLANT CREATED BY INCENTIVE-INSPIRED CO-ACTION IN DEVELOPING POSSIBILITIES IN PRODUCT

© LE Co. 1951

MACHINE BUILDER SIMPLIFIES SERVICE PROBLEMS WITH WELDED STEEL

By **Walter E. Vuchnich**
Welding Engineer
Milwaukee, Wisconsin

IN the original design of the side frame member (Fig. 3), service replacement of babbitt bearings for cross shafts required extensive disassembly of the machine. With the welded design (Fig. 4), however, it is now possible to utilize self-aligning roller bearings held in retainers that are simply bolted to the side frame member. All servicing can be done on the job with minimum down time.

The present welded design also affords other important benefits in manufacture. 49% less metal is needed. What little machining is necessary is done on light, fast machine tools. Shop assembly is simpler and can be done with fewer man-hours.

The shop cost of the welded steel frame is 21% less than that of the original construction.

Fig. 3—Original construction *of side frame member weighed 47 pounds and required larger machine tools for boring the retainers for babbitt bearings.*

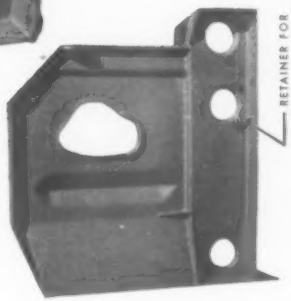


Fig. 4—Present welded steel design. *Side members are formed from sheet steel. Individual retainers for self-aligning bearings are held to sides with three bolts. Photos courtesy The Fox River Tractor Company, Appleton, Wisconsin.*

PROPER DESIGN IN WELDED STEEL ALWAYS IMPROVES PRODUCT AND LOWERS COST

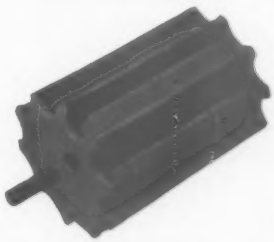


Fig. 1—Original design *of machine feeder roll required 63 pounds of iron.*

STRONGER, MORE RUGGED

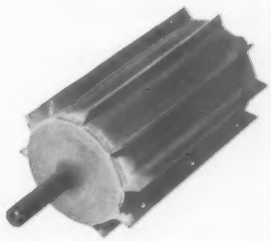


Fig. 2—Weldesign in steel takes 55% less material. *Costs 45% less to build. Eliminates former breakage in service.*

HERE'S MORE PROOF

Machine Design Sheets are available to designers and engineers. Simply write on your letterhead to Dept. 92.

THE LINCOLN ELECTRIC COMPANY
CLEVELAND 17, OHIO

Mk-h 100%
Pk-h 100%
Se 100%
Tr 100%
ED 100%

THE FORMULA
THAT ALWAYS MAKES GOOD

All-State

ALLOYS AND FLUXES
FOR WELDING BRAZING SOLDERING TINNING CUTTING

It's the formula that accounts for the high rate
• in application speed
• in uniformity
• in operator training
• in dependability
• in quality
which characterize the wide use of ALL-STATE Alloys and Fluxes.

It's the formula we use continuously. Interpreted:
Mk-h — Metallurgical know-how
Pk-h — Practical know- how
Se — Service
Tr — Training
ED — Excellent Distributors

A-S DISTRIBUTORS EVERYWHERE
Ask for a free copy of 32-page Buyers Guide to the complete line of ALL-STATE Alloys and Fluxes.
ALL-STATE
WELDING ALLOYS CO., INC.
White Plains, N. Y.

Co., Detroit. Industrial Electrical Engineering Society Representative—V. H. Chisholm, AWS, Hudson Motor Car Co., Detroit.

Social Get-Together

Fort Wayne, Ind.—"God of Creation" a sound and color film and an excellent picture was shown through the courtesy of the Moody Bible Institute, Chicago, at the December 6th dinner meeting of the *Anthony Wayne* Section. The film explained in an interesting, easy to understand language, the marvels of astronomy, natural science and the microscopic world. Fascinating lapse-time photography showing buds of flowers becoming full blossoms in a few seconds, also, the living organisms which inhabit the world were enlarged millions of times. It was a beautiful color picture and the time of 37 min. made it a well-worth-while film.

Round and square dancing with music by Earl Walker made for a most enjoyable evening.

Rectifier Welder

Grand Rapids, Mich.—G. K. Willecke of the Miller Electric Mfg. Co., Appleton, Wis., presented an excellent research talk on "A Rectifier Type D.-C. Welder" at the December 17th meeting of the *Western Michigan* Section held in the Junior College Bldg., this city.

Due to a severe blizzard the attendance at this very interesting meeting was low.

Chicago Bridge Film

Houston, Tex.—Eighty-one members and guests of the *Houston* Section were present at the December 14th dinner meeting to view a film produced by the Chicago Bridge and Iron Co., entitled "Shop Fabrication Procedures." This film, taken at the Chicago Bridge Birmingham plant, showed their procedures in the manufacture of tanks and pressure vessels starting with the arrival of the plate at their plant and following through the pickling, layout, forming, fabrication and welding, testing, and inspection procedures. S. C. Hamilton, Chairman of the Houston Section, and local sales manager for Chicago Bridge and Iron Co., furnished the commentary with the film. The members and guests appreciated and enjoyed this film immensely and they hope that they may be able to view similar films in the future.

The Houston Section is continuing its program of inviting student guests and at the meeting they had five young men from colleges in the area as guests.

Hard Surfacing

Milwaukee, Wis.—Howard S. Avery, AWS, Metallurgical Research Engineer, American Brake Shoe Co., Mahwah, N. J., called attention to the fact in his talk on "Hard Surfacing by Fusion Welding," that hard surfacing often provides average savings of several hundred per cent in material cost. Indirect savings (handling, shut-downs, etc.) may also be high. Besides the obvious use to protect parts subject to severe wear, Mr. Avery also pointed out that important savings can result from improvising emergency and temporary tools. Many of the merits of hard facing stem from the special materials used. Ordinary weld deposits range up to about 200 Brinell, hardened steels up to 700–800 Vickers and special carbide of hard-facing alloys up to about 3000 Vickers. Mr. Avery also covered the welding techniques and their advantages. His talk was supplemented with slides.

All of this interesting information was given at the Dec. 14, 1951, dinner meeting of the *Milwaukee* Section held at the Ambassador Hotel, this city.

Mr. Avery is a member of the AIME, ASM, British Iron and Steel Institute and the AWS. He has served on various executive, educational, specification and handbook committees. He was Chairman of the Technical Research Committee and a member of the Board of Directors of the Alloy Casting Institute from 1947 to 1950. At present he is chairman of the AWS Filler Metal Subcommittee on Hard Facing Alloys.

The meeting dates for the next four meetings of the Milwaukee Section are as follows: February 22nd, March 28th, April 25th and the Annual Party on May 24th.

Pressure Vessel Codes

Newark, N. J.—H. C. Boardman, AWS, Director of Research, Chicago Bridge and Iron Co., Chicago, Ill., gave a very excellent talk on the subject of codes, and in particular gave a great deal of behind-the-scenes information on how codes are compiled, the working of code committees, and the difficulties encountered in reaching agreement on codes. This took place at the regular monthly dinner meeting of the *New Jersey* Section held on December 18th at Essex House, Newark. Dr. Boardman also gave two very interesting case histories in regard to attempts to formulate codes for layer and forge constructed vessels. His talk contained a great deal of advanced information on the new 1952 codes including new arrangements and lettering system to cover the various methods, materials, etc.

An excellent film, "Silent Service"—submarines in the Pacific during World War II—was shown through the courtesy of the U. S. Navy.

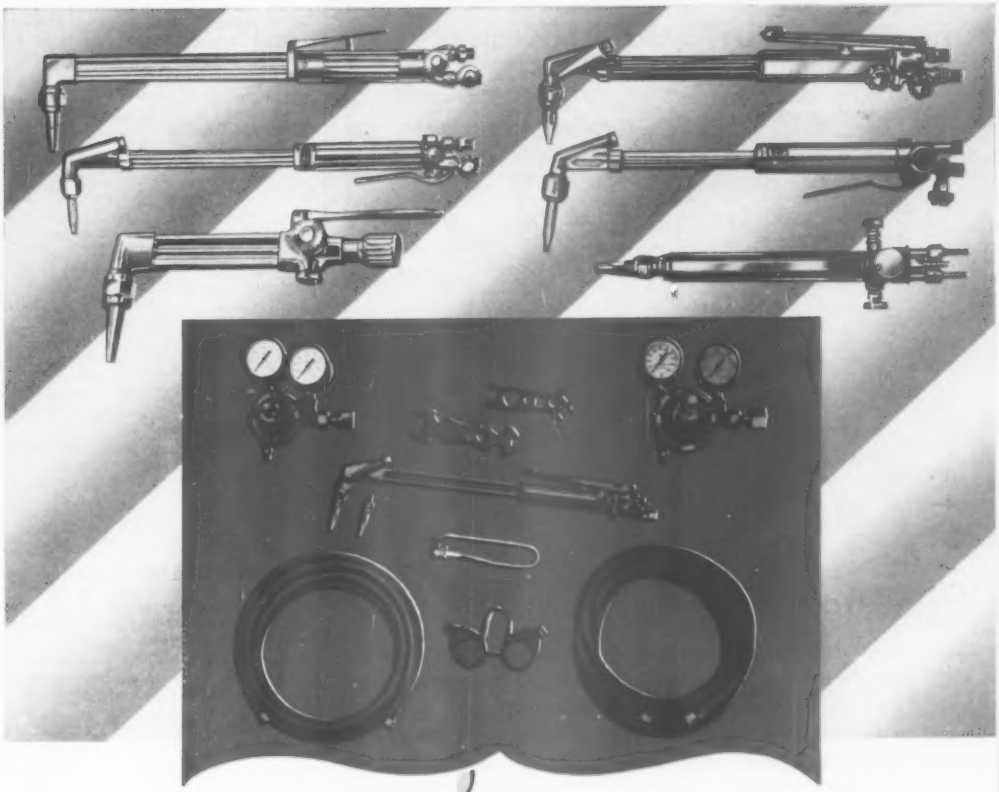
After the meeting snacks, refreshments and social hour followed.

Brazed Piping Systems

New York, N. Y.—An extemporaneous talk on "Brazed Piping Systems for Industrial and Building Installations" was given by Lewis G. Pick of the Walworth Co., this city, at the Dec. 11, 1951, dinner meeting of the *New York* Section held in Schwartz's Restaurant. Motion pictures were used to illustrate this excellent talk.

Off Shore Drilling Structures

Pascagoula, Miss.—Due to the inability of J. A. Campbell, Assistant to Executive



Answering your every WISH on welding and cutting problems.

★ ★ BURDOX ★ ★

every product... and every process... to fit every purpose best!

Work cannot wait today... especially work that calls for welding or cutting. It is vital, therefore, to find the best and fastest method to do every welding and cutting job. This is where BURDOX can help you. Since the BURDOX line includes products for every type of welding and cutting process, we know from experience which will do your job best. Moreover, in cases of shortages in one type of equipment, we can furnish an adequate substitute so that delays are forestalled and work continues. Every hour saved... every job handled faster means more output and more money to you. You can secure these benefits as well as added savings in time, money and delivery by securing all you need, all at once from BURDOX. Get acquainted with the extras BURDOX offers by writing for a new catalog today!

Branches
AKRON
MANSFIELD
COLUMBUS

THE BURDETT OXYGEN COMPANY

General Offices
3333 LAKESIDE AVENUE
CLEVELAND 14, OHIO

Plants
CLEVELAND & DAYTON, OHIO
LOS ANGELES, CALIFORNIA

Branches
CINCINNATI
YOUNGSTOWN

INDUSTRIAL GASES • WELDING AND CUTTING EQUIPMENT • ARC WELDING EQUIPMENT • SOLDERING AND BRAZING OUTFITS
REGULATORS AND GAUGES • HOSE AND CABLE • ACETYLENE GENERATORS • GOGGLES AND HELMETS • LENSES AND FACE SHIELDS
WELDING ELECTRODES • CYLINDER TRUCKS • AND MANY OTHERS

this new WELDING DESIGN MANUAL

FREE

FOR THE ASKING

72 PAGES
PROFUSELY ILLUSTRATED

Low Temperature
EUTECTIC
WELDING ALLOYS

To assist you in your re-tooling problems . . . or to help you analyze your present welding methods so that you may enjoy maximum speed, efficiency, and economy in many of YOUR production problems . . .

Giant new 4th Edition contains 62 new photographs, 132 new drawings, 72 pages of helpful data covering basic and advanced welding techniques and designs used in fabricating and assembly. Profusely illustrated with application drawings; weld diagrams; tables of melting temperatures, strengths, corrosion factors; charts; alloy recommendations; etc. Convenient digest size.

This just-off-the-press book is "must" reading for anyone engaged in defense production and design, and will certainly be time profitably spent for any production man.

SEE . . . The unbelievable savings in metal-joining which can now be yours through the use of "Low Temperature Welding Alloys"® discovered a few years ago and now used in over 78,000 industrial plants throughout America for more efficient metal-working production as well as for salvaging irreplaceable tool and machine parts.

Over 100 different, new, EUTECTIC Low Temperature WELDING ALLOYS® and EUTECTOR® Fluxes are job-engineered for use on ALL metals – cast iron, alloy steels, aluminum, copper and nickel alloys, die castings, overlays, etc., and may be applied with ALL heating methods – torch, arc, furnace, induction heating, etc.

SEND FOR YOUR COPY TODAY!

EUTECTIC WELDING ALLOYS CORPORATION
172nd Street at Northern Boulevard • Flushing, New York, N. Y.

EUTECTIC WELDING ALLOYS CORPORATION
WJ-2 172nd St. & Northern Blvd., Flushing – New York, N. Y.

This new manual of yours sounds like a very helpful book. Send me a FREE copy with the understanding that there will be no cost or obligation now or later.

*Signed*_____
*Firm*_____
*Address*_____ *Zone*_____
*City*_____ *State*_____

Vice-President, Ingalls Shipbuilding Corp., to be present, his paper on "Off Shore Drilling Structures" was presented by Paul M. Tunnison, a former associate of Mr. Campbell, at the December 5th dinner meeting of the *Pascagoula* Section held at the American Legion Post, this city. Some 42 members and guests followed the talk with interest.

Hard Facing

Phoenix, Ariz.—Edward Rogers, AWS, Chief Metallurgist, Stoody Co., Whittier, Calif., a very well qualified speaker, gave an enlightening talk on "Factors in Hard Facing" at the Wednesday, Dec. 19, 1951, meeting of the *Arizona* Section held in the Saratoga Room of the Hotel Westward Ho, this city. Mr. Rogers not only covered the theoretical aspects of hard facing, but also showed the many practical applications used today. Following Mr. Rogers' talk a question period was held which brought out more information on specific features. All agreed it was a very informative talk and all appreciated the Stoody Co.'s courtesy in sending Mr. Rogers to speak before this Section.

Tool and Die Welding

Pittsburgh, Pa.—Speaker at the Nov. 21, 1951, dinner meeting of the *Pittsburgh* Section held at Mellon Institute, was Pat S. Doyen, AWS, Engineer, Welding Equipment and Supply Co., Detroit, Mich. New methods were covered by Mr. Doyen in his interesting talk on the subject, "Engineering Aspects of Tool and Die Welding," which required an hour and a half for its presentation. A discussion lasting 30 min. followed the presentation.

A film, "Stainless Steel," was shown through the courtesy of the Allegheny-Ludlum Steel Co. and was very well received.

Inert Arc Welding

Rochester, N. Y.—John McKenzie of The Linde Air Products Co., assisted by G. A. Kazanjian, gave a very interesting preview of the above subject and its many forms of applications and accomplishments to date, at the Dec. 17, 1951, meeting of the *Rochester* Section, held at the William J. Meyer Co. plant. A practical demonstration was conducted on numerous alloys that proved of interest to all. A question-and-answer session followed and many interesting discussions developed.

The Section wishes to take this opportunity to thank the William J. Meyer Co. (welding and fabrication specialists) and staff for its whole-hearted effort and courtesy shown during the plant inspection trip, and the fine coffee and doughnuts served. Seventy members attended the meeting.

Prior to the meeting and inspection trip, dinner was served to approximately 37 members and guests at the Rochester Turners Club. A coffee talk was given by Herman Walz, of Walz and Krenzer, an official of the Blood Bank. Mr. Walz

Train Your Operators Better and Faster with G-E's Movie "The Inside of Arc Welding"

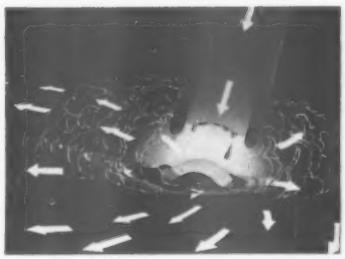
Full Color and Sound Movie Speeds Training Programs up to 20%

Teach beginners the fundamentals of arc welding quickly with the color movie, "The Inside of Arc Welding." Produced during World War II, this movie speeded up training of operators as much as 20 percent. The film enables instructors to give even the largest classes many of the advantages of individual attention.

Authentic photographs and animated cartoons show all details of the arc-welding process: current setting, angle of electrode, length of arc, electrode travel speed. You can see good and bad welds in cross section, and watch experienced operators demonstrate principles of arc welding in *all* welding positions.

In Six Parts—each part 10 minutes and complete in itself (16mm color and sound)

1. *Fundamentals*
2. *Flat position*
3. *Horizontal position*
4. *Flat and horizontal with AC*
5. *Vertical position*
6. *Overhead position*



Animated drawings illustrate the arc and molten pool in terms easy for the beginner to understand

You Can Buy These Films at Cost for your Training Programs. "The Inside of Arc Welding" costs only $55 per part, or $330 for a complete set of six parts. "The Story of AC Welding" costs $95 for Part I and $105 for Part II. Write to Section 712-14, General Electric Company, Schenectady 5, N. Y.

You Can Borrow Prints, free of charge, for single showings. Just get in touch with your authorized G-E Welding Distributor. You can find his name by looking for General Electric under "Welding Equipment" in the yellow pages of your telephone directory.

"THE STORY OF AC WELDING"

Shows You Why AC Is Best for Three out of Four Welding Jobs!

"The Story of AC Welding" explains how you can improve weld quality, increase welding speeds and reduce costs with AC welders.

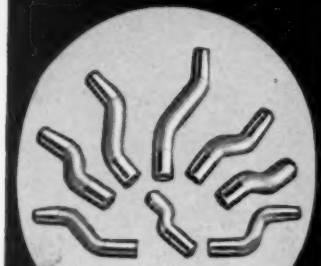
You see clearly, by means of actual photographs and animated cartoons, why and how AC eliminates troublesome arc blow. You see the arc's action in corners, angles and intersections. The arc and molten pool are shown in close-ups. Part I shows actual operation of the arc while Part II shows advantages of AC. Each part is in full color and sound.

The film shows the causes and effects of troublesome arc blow, and how AC eliminates it.

GENERAL ELECTRIC

712-14

For Longer Runs and Lower Costs



. . . resistance-weld with AMPCO WELD cold-bent tips

Ampco Weld cold-bent offset tips are made without patterns or special dies. Cold bending retains all physical properties too. Moreover, because holes bend with metal, there's no special drilling needed for water passages. That's why you get prompt service and fast delivery when you use Ampco Weld cold-bent tips.

But that's not all you get. You get longer runs and lower costs too — because the water passage gives you cooling right to the welding face. So, place your order today and enjoy all these advantages.

. . . and AMPCO Engineering Service when you need it

Behind the famous Ampco Weld line of resistance-welding products is a corps of experienced engineers, ready to help you solve your problems. Just get in touch with us.

AMPCO METAL, INC.
Dept. WJ-8
MILWAUKEE 46, WISCONSIN

It's Production-Wise to Ampco-ize

AW-2

gave a very impressive 20-min. talk on the urgent need of blood. He pointed out that it is every able-bodied persons' patriotic duty to donate as often as possible to aid in the saving of lives at home and in the armed forces. To date, Mr. Walz has given 22 pints of his blood and is looking forward to giving many more.

Entertainment

Saginaw, Mich.—Hilarious amusement was furnished by Joe Hanneford, the World's Most Famous Clown, at the dinner meeting of the *Saginaw Valley* Section held on November 13th at Zehnder's Hotel, Frankenmuth, Mich., with an attendance of 102 members and guests.

Low-Hydrogen Electrodes

South Bend, Ind.—D. L. Mathias, AWS, Research Engineer, Arcrods Corp., Sparrows Point, Md., was the guest speaker at the joint meeting of the *Michiana* Section with the Notre Dame Chapter ASM, held on Dec. 12, 1951, in the Engineering Bldg. at Notre Dame. In his paper on "Lime-Coated Ferritic Electrodes" Mr. Mathias showed that low-hydrogen, lime-coated ferritic electrodes have some definite advantages over the older cellulose types. Welds can be deposited on high sulphur steels, hardenable steels, complex alloy steels, etc. This type of rod can be used with both a.-c. and d.-c. setups. These low-hydrogen electrodes consistently deposit high quality welds under adverse conditions. There is no underbead cracking on hardenable steels to contend with, reaction pitting is reduced to a minimum and flaking and bubbles along the weld on enameled weldments are eliminated with these low-hydrogen rods. It takes time to train operators on these electrodes.

The coating on these rods tends to absorb moisture during storage, but this can be eliminated if they are stored at 225° F. or higher. They are packaged carefully in sealed containers and can be maintained in a sufficiently dry condition to give no difficulty. Restrained welds have micro fissures if moisture is not held down; however, unrestrained welds are not so greatly affected.

Mr. Mathias mentioned current research developments in the field in the direction of a ceramic bond instead of sodium silicate to reduce moisture pickup. However, this product is not yet available on the market for general use.

Welding Processes

Washington, D. C.—Leon C. Bibber, AWS, Welding Engineer for the United States Steel Co., Pittsburgh, Pa., put on a very good show in connection with his talk on "Welding Processes and Their Relationship to Design and Fabrication," at the Oct. 24, 1951, dinner meeting of the *Washington* Section. Mr. Bibber's extemporaneous remarks were excellent and the information he gave was of interest to the executive, the engineer and the operator, as well.

THE Right PROTECTION

is DOCKSON GOGGLES

BE SAFE against sparks, dust particles, chemical splash and fumes, glare and injurious rays with DOCKSON GOGGLES in more than 20 models and a full line of modern lenses for all hazards.

BE COMFORTABLE with smooth-sitting DOCKSON GOGGLES. Excess weight is engineered out.

BE ECONOMICAL, get longer use from DOCKSON GOGGLES. "BUILT FOR BETTER SERVICE".

THERE IS A DOCKSON DISTRIBUTOR NEAR YOU — Let us send you his name and our complete catalog of DOCKSON HEAD AND EYE PROTECTION.

Dockson CORPORATION
3839 WABASH • DETROIT 8, MICH.

The entire surface of the 5-ft. long mixer blades was coated with HAYSTELLITE tung-sten carbide tube rod, applied by the metallic-arc process. This procedure increased the life of the blades from five weeks to 20 months.

The hard-faced blades on this 21 cu. ft. mixer have already lasted 20 times as long as cast steel blades, mixing 75 cu. yd. of concrete daily, 6 days a week.

HARD-FACING

Saves 19 Shutdowns

Cast steel concrete mixer blades, used in the production of concrete blocks, had to be replaced every 4 or 5 weeks when running 3 shifts a day, 6 days a week. Besides the high cost of the new blades, there was the added expense of installation and lost production time while the mixer was being repaired.

The life of the blades was increased to at least 20 months, by coating their surface with a ¼-in. deposit of HAYSTELLITE tungsten carbide tube rod by the metallic-arc process. The long service life of the hard-faced blades helps to maintain continuous production without frequent and costly shutdowns for the replacement of worn blades.

For complete information on hard-facing, including descriptions of the various HAYNES alloy rods and the method of selecting the right rod to combat your wear problem, write for the 40-page booklet, "HAYNES Alloys—Hard-Facing Manual."

HAYNES TRADE-MARK *alloys*

"Haynes" and "Haystellite" are trade-marks of Union Carbide and Carbon Corporation.

Haynes Stellite Company
A Division of
Union Carbide and Carbon Corporation
UCC
General Offices and Works, Kokomo, Indiana
Sales Offices
Chicago — Cleveland — Detroit — Houston
Los Angeles — New York — San Francisco — Tulsa

Fabrication Designing

Washington, D. C.—Charles D. Evans, AWS, General Supervisor of Welding Research for the International Harvester Co., Chicago, gave a semitechnical talk on methods and applications in the designing of welded fabrication for armored vehicles at the November 28th dinner meeting of the *Washington* Section.

Section Reorganization

York, Pa.—The *York-Central Pa.* Section announces the election of officers for the fiscal year as follows:

Chairman—Charles H. Pyle, Lukens Steel Co., (Res.) 777 Grandview Rd., York, Pa.
1st Vice-Chairman—Frank Logue, Alloy Rods Co., Lincoln Highway West, York, Pa.
Secretary—John Kugle, A. B. Farquhar Co., York, Pa.
Asst. Secretary—Miss Emily Light, 447 W. Market St., York, Pa.
Treasurer—Charles W. Allen, A. B. Farquhar Co., (Res.) 132 W. Jackson St., York, Pa.
Chairman, Membership Committee—Gordon Appleby, Lincoln Electric Co., (Res.) 1233 E. Maple St., York, Pa.
Chairman, Program Committee—Wm. De Long, The McKay Co., York, Pa.
Technical Representative—Richard Lee, Alloy Rods Co., (Res.) 521 Colonial Ave., York, Pa.

The York-Central Pa. Section has been going through the throes of reorganization. Due to changes in industry and personnel location, this Section suffered from a loss of officership. The new officers are interested people who, cognizant of the great need for continuation of the activity considering the loyalty of the area's membership, joined together and have reorganized on the basis of the slate shown above. Plans are being made for a joint meeting with the ASM on February 13th for a lecture on Welding Metallurgy, a social Oyster Bake activity on March 5th, the showing of the AWS slide strip film on Applied Welding Engineering in April and a plant visit in May. The last activity of the Section was a plant visit to the Reed Standard Corp. John I. Spangler, Plant Manager, gave an excellent talk on the fabrication of chemical and food mixing equipment.

It's No Secret Practically everybody in the field knows . . .

MANGANAL

U.S. Patents 1,876,738—1,947,167—2,021,945

11%-13¼% MANGANESE-NICKEL STEEL APPLICATOR BARS AND WELDING ELECTRODES are used to...

• REBUILD WORN HAMMERMILL HAMMERS Quickly and More Economically!

- MANGANAL usually outlasts new hammers.
- MANGANAL practically eliminates build-up time.
- MANGANAL is the toughest steel known — thrives under impact and abrasion.
- MANGANAL workhardens to 550 Brinell.
- MANGANAL has a tensile strength of up to 150,000 p.s.i.

REDUCE COSTS STILL MORE . . . by using a single pass of SEACO hard-surfacing electrodes. It allows the steel to workharden before wear hits it.

FREE Helpful literature on how to reduce costs on repairs of Quarry, Mining and Contractors equipment.

NAME OF NEAREST DISTRIBUTOR ON REQUEST

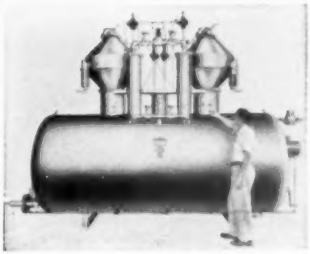
Send for catalog and price list.

STULZ-SICKLES CO. SOLE PRODUCERS 96 N. J. Railroad Ave. Newark 5, New Jersey

BIGGEST ACETYLENE PLANT NEWS IN 25 YEARS!...

Sight Feed

MODEL A-TWIN ACETYLENE GENERATORS



AND THE

SIGHT FEED "M-1" ACETYLENE COMPRESSOR

Sight Feed Acetylene Compressing Plants represent the biggest advancement in design and construction of cylinder filling equipment in more than 25 years.

HERE'S WHY:

- Acetylene is produced by the Model A-Twin Generator, which automatically supplies a CONTINUOUS flow of the purest acetylene obtainable. No shutting down for re-charging. No waste of acetylene. Simple, economical operation.
- Sight Feed Compressing Plants do not require gas holders. This means lower initial cost and less maintenance.
- Model "M-1" Acetylene Compressors are compact, light in weight, specially designed and improved for efficient operation. They feature built-in units formerly constructed as separate pieces of equipment, thus simplifying operation.

Sight Feed Complete Acetylene Compressing Plants are described in a new Bulletin CP-152. Write for your copy today!

THE SIGHT FEED GENERATOR COMPANY

West Alexandria, Ohio, U.S.A.

Manufacturers of Welding Equipment For More Than 25 Years

WELDING Sight Feed EQUIPMENT

NEW LITERATURE

Nickel Alloy Steel Castings in Industry

This 32-page bulletin contains over 100 illustrations. It reports on steel, cast to shape, as a reliable engineering material. Cast steels meet exacting service requirements and the continued demand of industry for increased power, higher speeds, heavier loads and longer service life in machinery and equipment parts that can be made most economically and efficiently in cast form. Ample strength and toughness are assured by the intelligent use of nickel and other alloying elements, and their inherent qualities can be modified and supplemented by suitable heat treatment. Data on properties and applications of cast nickel steels of the constructional grades are classified by industrial fields. Low-temperature properties, abrasion resistance, depth hardening and welding are discussed. Advantages and recommended specifications and compositions for typical service applications are given. International Nickel, Dept. EZ, New York 5, N. Y.

The Working of Aluminium in the Shipyard

The bulk of this publication is divided into four sections, viz., the working of platers material; sheet metal work; machining; methods of joining. The last section includes very recent information concerning argon arc welding in addition to the older methods. Recommendations are given on treatment, where necessary, of aluminium in contact with other metals, with timber and concrete, etc. There are also sections on painting, antifouling compositions and deck compositions. There is a brief reference to methods of storing heavy sections and thin material and on the identification of aluminium alloys. Three Appendices deal, respectively, with (1) aluminium alloys recommended for marine work, (2) British Standards and the available forms and sizes in which aluminium is supplied and (3) a note on the heat treatment applied to appropriate materials in order to improve their strength and other properties. Copies of Bulletin No. 18 may be obtained by writing to The Aluminium Development Assn., 33, Grosvenor St., London, W. 1, England.

New X-ray Diffraction and Spectrometer Catalog

A new 60-page catalog titled "X-ray Diffraction and Geiger-Counter X-ray Spectrometric Equipment" has been released recently by the Research & Control Instruments Div., North American Philips Co., Inc., 750 S. Fulton Ave., Mount Vernon, N. Y.

In addition to X-ray diffraction, spectrometry and fluorescence analysis, the catalog covers such components and accessories as tubes, rectifiers and cameras. It also has sections on camera mounting brackets, film illuminators and measuring devices, and monochromators.

Booklet on Hobart Brothers Plant

A 40-page photo booklet showing facilities and production operations in their plant is being released by Hobart Brothers Co., Troy, Ohio. This photo booklet will be mailed free of charge to anyone interested in the modern production of electrical equipment.

Ampco Wall Chart

A colorful wall chart listing all of the Ampco bronze weldrod and wire products available by trade name, together with AWS-ASTM designation, has just been released by Ampco Metal, Inc.

In addition, and opposite the trade names, is descriptive information, typical applications, mechanical properties, deposit chemical composition, recommended current and polarity, NEMA color markings and diameter sizes.

Also included is a copy of the "Bronze Electrodes Selection and Preheat Chart" making an easy and convenient reference and check list.

The chart is bordered with numerous application pictures with a brief description of each specific job.

Copies of this wall chart will be supplied gratis by writing Ampco Metal, Inc., 1745 S. 38th St., Milwaukee 46, Wis.

Welding Alloys Directory

A new, 16-page, profusely illustrated *1952 Directory* of electrodes and welding alloys has been issued by Eutectic Welding Alloys Corp., Flushing, N. Y.

The new booklet contains photographs, data and technical information on a series of "Eutectic Low-Temperature Welding Alloys" for use on cast iron, steel, alloy steels, copper, brass, bronze, aluminum, magnesium, hard and machinable overlays and for cutting all metals.

In addition, two full pages are devoted to a full chart of over 100 of Eutectic's different welding alloys, their main uses, their bonding temperatures and their tensile strengths plus many valuable ideas on effective uses of welding.

Copies of this new Directory may be obtained without charge by writing Eutectic Welding Alloys Corp., 172nd St. and Northern Blvd., Flushing 58, N. Y.

ALCOA An American Enterprise

Alcoa an American Enterprise by Charles C. Carr, illustrated with photographs and maps.

Charles Martin Hall is one of the great names in American industry. On Feb. 23, 1886, at the age of 22, he solved a problem which had been baffling scientists for three quarters of a century. His achievement turned a small company of young visionaries into one of the world's greatest corporations.

Nature made aluminum light. Research made it strong. Hall's discovery of a cheap method of turning bauxite into aluminum by electrolysis is one of the most fascinating stories in American industry. The son of a Congregational minister, educated in a small liberal arts college in Ohio, he was a true storybook inventor—young, poor, inspired.

Alcoa was built on the research that Hall began in a woodshed in Oberlin, Ohio. Here is the full story, from its pioneering mill plant days in the late '80's to the present. Here are the early struggles for patent rights, for financial backing, for consumer markets. Here is the scientific research, and the business genius, the rumors, the gossip, the facts.

The history of Alcoa is similar in many ways to that of other great American industries. Yet it has met situations for which it would be difficult to find a parallel.

Few people could have written this history of the nation's first and largest producer of aluminum with a reservoir of information equal to that compiled by its author, Charles C. Carr, as director of public relations for Alcoa through fifteen of its most exciting years.

Price $3.50. Rinehart & Company, Inc., New York and Toronto.

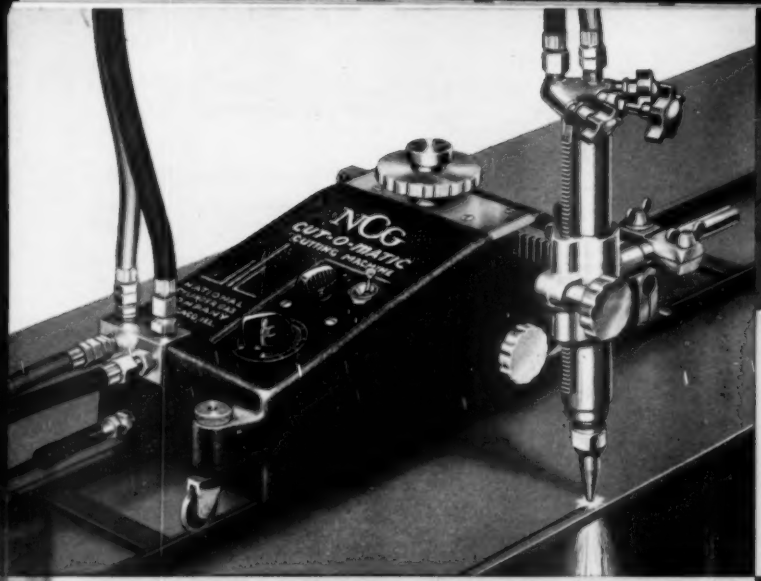
Materials Technology for Electron Tubes

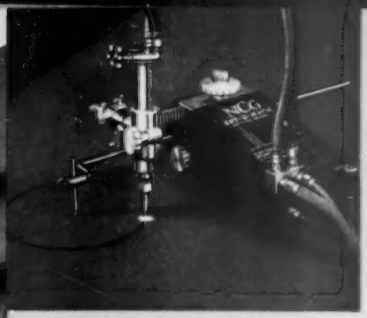
This book, by Walter H. Kohl, Consultant to the Director of Research, Collins Radio Co., Cedar Rapids, Iowa, presents up-to-date information on materials used in the construction of electron tubes. It is the first one in English which aims at a thorough scientific treatment of the main solids which enter into the construction of electron tubes.

The treatment of materials such as glass, ceramics, mica, tungsten, molybdenum, tantalum, copper, nickel and graphite is one feature of the book.

Of particular interest to the welding engineer is a comprehensive chapter on "Joining Metals by Soldering and Brazing." This chapter consists of 48 pages of technical data, tables and scientific information.

Of special interest, also, is the chapter on "Glass, Ceramic and Metal Sealing." The 6 by 9 book contains 493 pages and is priced at $10. The Reinhold Publishing Corp., 330 W. 42nd St., New York, N. Y.





CIRCLES AND ARCS

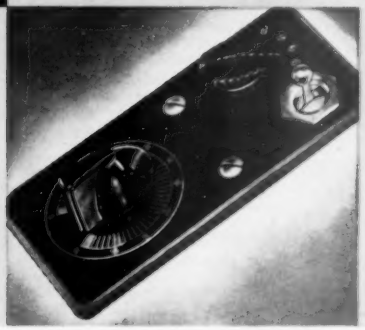
Circles or arcs, up to 60-in. in diameter, are cut by attaching radius rod assembly. Larger diameters may be cut with extension rod. Rings may be cut with double torch mounting.

SIMPLE CONTROLS

The Cut-O-Matic Control Panel is simple. A selector switch sets the travel at one of four speeds, and a rheostat permits accurate regulation anywhere between 4-in. and 60-in. per minute, in either direction.

CUT-O-MATIC

NCG'S PORTABLE, MOTOR-DRIVEN FLAME-CUTTING MACHINE



LOW-COST, VERSATILE, ACCURATE, FAST

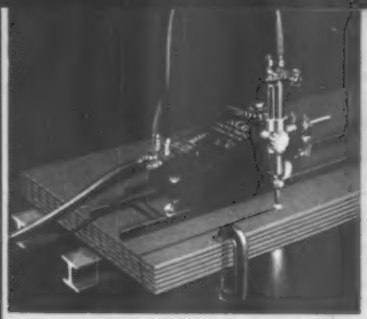
Flame-Cutting with Substantial Savings in Labor and Materials

Here's a small, inexpensive cutting machine that's so efficient that grinding or machining of cuts are rarely necessary . . . That's so versatile you can cut light plate or heavy slabs—straight lines, bevels or various shapes . . . That's so portable you can take the machine to the job—inside or outside the shop, wherever power is available. And it has exclusive operating features that make it remarkably easy to handle.

Yes, the NCG Cut-O-Matic is so efficient and so flexible in use that one man and a Cut-O-Matic make a hard-to-beat team to save time, labor, and materials.

The Cut-O-Matic is not only a money saver in the larger plant but in small shops, too. In countless instances the low price of the Cut-O-Matic has enabled small businesses to enjoy the recognized superiority of machine cutting—with its sharper, cleaner cuts and its overall economies in time, materials and gas.

Cut-O-Matic offers so many advantages that you'll want a copy of the illustrated booklet describing the unit in detail. If you'd like a demonstration right in your own shop, just check the box in the coupon at the right.



STACK CUTTING

Stack cutting gives real production economy and speed. Sheets clamped together may be cut uniformly. Circles and arcs as well as straight lines are easily stack-cut.

STRIP CUTTING

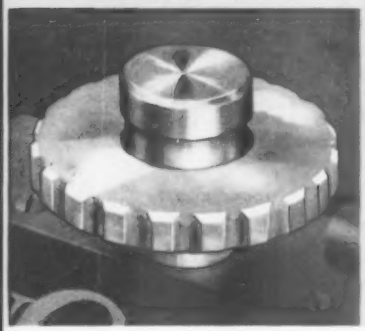
Strips up to 29-in. wide can be cut by adding torch holder, torch and hose assembly. Wider strips may be cut by using extra length equipment. Narrow strips may be cut by mounting two torches side by side.

IRREGULAR SHAPES

Limited irregular shapes may be cut by guiding the Cut-O-Matic by hand. Rear wheel is castered to facilitate travel. Cuts are much better than those made with a hand cutting torch.

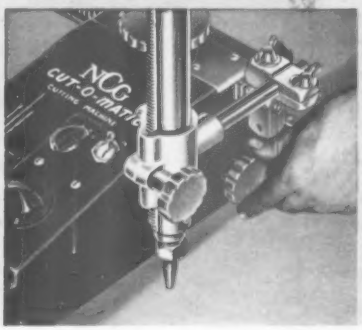
FREE-WHEELING

Fingertip clutch control is combined with a cross-feed gear rack adjustment to permit moving the machine on track while adjusting the cross-feed, in one simultaneous operation.



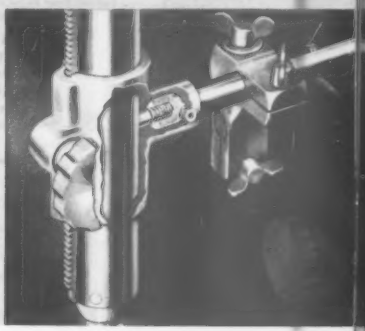
EASY CHANGE-OVER

It's easy to switch from cutting straight-line to circles. The friction knob is loosened, as illustrated, and the inside drive wheel is disengaged. The outside wheel then propels the machine around the center point.



SMOOTH—RIGID

No ragged cuts caused by excessive play and uneven rocking. A special clamp adjustment, plus a spring-loaded pressure plug, holds torch in rigidly, yet permits smooth rocking.



Actual photograph of bevel cut with CUT-O-MATIC. Clean, smooth, sharp edges held to close tolerances eliminate need for machining or grinding.

NCG CUT-O-MATIC STANDARD UNIT

F.O.B. FACTORY, LOUISVILLE, KY

$210

Complete with torch, tip, radius rod, track, base, table and torch wrench

CUT-O-MATIC possesses all the quality characteristics of famous NCG heavy-duty flame-cutting equipment. Sturdy, simply designed for efficient operation and easy maintenance, the CUT-O-MATIC is a dependable tool that will give you long trouble-free service.

NATIONAL CYLINDER GAS COMPANY

NCG®

EVERYTHING FOR WELDING

"Cut-O-Matic" and "NCG" are trademarks.

Executive Offices:

858 N. Michigan Avenue, Chicago 11, Illinois

NATIONAL CYLINDER GAS COMPANY
858 N. Michigan Ave., Chicago 11, Ill.

Please send me Illustrated Booklet N-134 describing in detail the versatile CUT-O-MATIC Flame-Cutting Machine.

NAME_____ POSITION_____

COMPANY_____

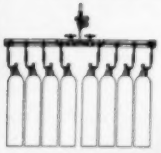
STREET ADDRESS_____

CITY_____ ZONE_____ STATE_____

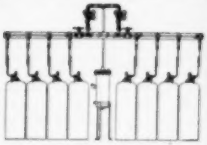
☐ I would like a demonstration in my own shop.

MANIFOLDS

REGO

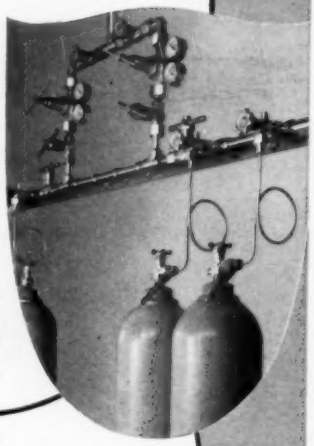


Typical Single Regulator Wall-Type Oxygen Manifold



Typical Dual Regulator Floor-Type Acetylene Manifold

for OXYGEN
ACETYLENE
HYDROGEN
NITROGEN
and other high pressure gases



Rugged construction and precision control equipment give RegO manifolds long life, safe performance and low maintenance.

Continuous Operation . . . Shut-off valve at each cylinder station permits removal of any cylinder without shutting down entire side of manifold. Master valves control each bank of cylinders.

Unit Construction . . . Header consists of steel I-beam, extra heavy brass pipe and fittings, cylinder station and master shut-off valves . . . with all permanent connections silver brazed.

Precision Regulation . . . Uniform delivery pressure is assured by dual large capacity two-stage Regulators.

REGO

The BASTIAN-BLESSING Company

4201 West Peterson Ave. Chicago 30, Illinois

PIONEER AND LEADER IN THE DESIGN AND MANUFACTURE OF PRECISION EQUIPMENT FOR USING AND CONTROLLING HIGH PRESSURE GASES

*Reg. U. S. Pat. Off.

Aluminum Powders and Pastes

Aluminum Powders & Pastes is a new 84-page manual just published by Reynolds Metals Co., 2500 S. Third St., Louisville, Ky. It covers not only the more familiar uses of powdered aluminum in aluminum paints, irridescent auto finishes, roof coatings and the like, but also describes such unique applications as chemical reactions which provide heat without flames, sea markers that help locate downed airmen, bombs whose destructive power is doubled by powdered aluminum, powder metallurgy, surgery, silicosis treatment and other amazing developments.

Many important applications of aluminum powders have barely been touched upon so far...and there are new ones still in the "dream" stage. With aluminum powders thus becoming of increasing importance, there is need for a compact source of information on the various powders, their characteristics, methods of control and testing with recommendations for their use. All these are within the scope of the new book.

Employment Service Bulletin

Positions Vacant

Metallurgical Engineer: An outstanding man is required to do fundamental and applied research in soldering and brazing. One to three years' experience in joining techniques and alloy development would be an asset. Send complete details of scholastic and industrial experience, personal data, references and photograph direct to L. A. Cook, Division of Metallurgical Research, Kaiser Aluminum and Chemical Corp., P. O. Box 1451, Spokane 10, Wash.

V-264. Eastern Electrode manufacturer has excellent opening for young graduate metallurgist with welding experience or Welding Engineer with working knowledge of metallurgy. Work involves coating development, research and testing, etc. In reply state complete qualifications, age and salary desired.

Services Available

A-624. Engineer Executive: Age 41. Diversified experience in engineering and manufacturing in metal fabrication of ferrous and nonferrous metals. Former positions as Works Manager, Factory Manager, Chief Engineer and Chief Production Engineer. Detailed in Production Engineering, Production Control, Product Design and Development, Welding Engineering, Factory Cost and Sales Engineering. Ability to handle mass production, resourceful administrator. Present salary—five figures.

LIST OF NEW MEMBERS

Effective December 1, 1951

ARIZONA

Moore, John R. (C)

BIRMINGHAM

Gardner, E. V. (C)

BOSTON

Ciaramaglia, John (C)
Donahue, Ernest J. (C)
Malcolm, Edmund M. (A)

CHATTANOOGA

Arpi, W. A. (C)
Brackett, E. V. (C)
Carlson, D. C. (C)
Jones, J. W. (C)
Keller, E. D. (C)
Petty, F. T. (C)
Rule, B. W. (C)

CHICAGO

Schlifski, Herman (C)

CINCINNATI

Thayer, G. T., III (B)

CLEVELAND

Chambers, Robert (C)
Crobar, Elmer L. (C)
Given, Harry M. (B)
Henderson, Glenn (B)
Katz, Lewis (C)
Munn, John M. (C)
Nichols, S. C. (C)
Preskar, John (C)
Tharp, Elwood J. (C)
Varga, William (B)
Yarrow, Ross Jay (C)

COLORADO

Harrington, John W. (B)

COLUMBUS

Keeling, Charles S. (C)
Rupp, Fred B., Jr. (C)
Shively, Wayne (C)
Smith, Victor E. (C)
Troiano, Lewis (C)

DALLAS

Focht, Aaron (D)

DETROIT

Burch, Guy E. (B)
Colyer, Jack D. (B)
Cook, Earl C. (B)
Davies, C. V. (B)
Forsyth, James (C)
Ginter, William (B)
Jenkins, James E. (C)
Jones, William A. (C)
Kading, Albert K. (B)
Moore, William A. (B)
Mueller, F. (B)
Nagy, William J. (B)
Nalon, Gerald C. (B)
Oram, Paul (B)
Peters, Lewis J. (C)
Peters, William (B)
Pogue, Charles (B)
Renny, Charles (B)
Smolinski, Ted (B)
Stellman, Michael C. (C)
Wohlmuth, Calvin (B)

HOUSTON

Batson, Kenneth R. (C)
Ketchbaus, Thomas D. (B)
Rucker, Herman (C)
Victor, George F., Jr. (C)

INDIANA

Kelly, James W. (B)
McShan, Dorman (B)

LONG BEACH

Buchanan, Neil Warren (B)
Diehl, J. L. (B)
Kruse, Harold G. (C)
McClure, John S., Jr. (C)
Zuccone, A. E. (C)

LOS ANGELES

Crisman, Gerd (C)
Davis, Clifford G. (C)
Fleek, L. William (C)
Foss, Lloyd A. (C)
Gibbon, William D. (C)
Grohs, Fred W. (C)
Kinney, William Henry (B)
Laughlin, A. J. (C)
McGhie, Roland W. (C)
Michaud, Martin L. (B)
Moeller, John W. (B)
Oberlies, Marley J. (C)
Seddon, Joseph (B)
Steffey, Carl C. (C)
Todd, Ralph (B)
Zika, J. F. (B)

MAHONING VALLEY

Barca, Anthony (C)

MARYLAND

Frey, Richard R. (C)
Weiss, Rena S. (C)

MILWAUKEE

Niemczyk, Theodore J. (B)
Spencer, Theodore J. (C)
St. Aubin, John (B)
Wells, Cedric Albert (C)
Worden, Stewart W. (C)
Wright, William H. B., III (C)
Zettinig, Rudolf John (C)

NASHVILLE

Andrews, L. H. (B)

NEW JERSEY

Joy, R. W. (B)
Sullivan, Stephen L. (C)

NEW YORK

Bradfield, G. K. (B)
Guggenheim, S. Frederic (C)
Hamilton, George (B)
Nolin, Lasalle L. (C)
Quaas, Joseph F. (B)
Stephens, Olin J., II (B)
Vollack, Robert F. (C)

NORTHERN NEW YORK

Dawson, John Harold (D)

NORTHWESTERN PA.

Danner, George E. (B)

OKLAHOMA CITY

Freeman, John W. (D)

PASCAGOULA

Poe, M. O. (C)

PHILADELPHIA

Benwell, Robert J. (B)

PITTSBURGH

Day, Maurice J. (C)
Dowd, James D. (B)
Lamb, Kenneth (C)

PUGET SOUND

Lind, C. A. A. (C)
Zylstra, L. B. (B)

RICHMOND

Baker, Ralph E. (C)
Schultze, G. W. (B)

ROCHESTER

McKenzie, John S. (C)

SAGINAW VALLEY

Hierholzer, Adolph (C)
Longair, H. Wayne (D)

ST. LOUIS

Bethel, Gilbert Louis (C)
Greene, Fred (C)
Harrington, Kenyon V. (B)

SALT LAKE CITY

Cunningham, Robert J. (B)
Jensen, J. C. (B)

SAN FRANCISCO

Doggett, Cecil W. (B)
Heidt, John W. (B)
Wallace, William J. (C)

SUSQUEHANNA VALLEY

Luetzel, W. L. (C)

SYRACUSE

Banghart, Edwin L. (B)
Hoffman, Donald E. (C)
Miller, Harry E. (C)
Ritter, Harry L. (C)
Speers, Norman E. (C)
Sukert, Harold J. (C)
Tynan, Dan R. (C)

TOLEDO

Andrews, W. A. (C)
Asendorf, Paul W. (B)
Fisher, Oran R. (C)
Kittle, James C. (B)
Parish, Paul (B)
Price, S. G. (C)
Vogeli, Clarence A. (C)
Weide, H. W. (C)

TULSA

Church, Robert M. (B)

WASHINGTON

Duty, James M. (C)
Furr, James D. (C)
Gates, Albert L. (C)
Goodin, Gordon M. (C)
McKenzie, Joseph W. (C)
Parker, Victor T. (C)
Shoup, Paul W. (C)

WICHITA

Garter, Charles R. (D)
Duval, Carroll V. (D)
Goss, Horton (B)

WORCESTER

Firmani, Lidio (C)
Schwartz, Eino S. (B)
Stefanick, Mitchell F. (C)

YORK-CENTRAL PA.

Jennings, G. Earl (B)
Phillips, George (B)
Stumpf, Paul LeRoy (B)
Wilt, Luther A. (B)

NOT IN SECTIONS

Noren, Tore (B)
Pease, Ivan R. (B)
Rogers, R. T. (C)
Sands, John F. (B)
Thompson, Howard J. (B)
Wetak, Edward A. (C)

Members Reclassified

During the month of December

DETROIT

Baker, Lawrence (C to B)
Bedard, Harry W., Jr. (D to C)
Kramer, William P (C. to B)
Kroy, Walter (C to A)

LOS ANGELES

Wheeler, Lloyd F. (C to B)

NOT IN SECTIONS

Bernhardt, John V. (D to C)

Specify...
...WEIGER-WEED
for sound, clean welds!

(shown left)
WEIGER-WEED
Cold Formed
Offset Tip, Type D

WW

Standard Replaceable
Cold Formed Offset
WELDING TIPS

Through the development of special cold forming equipment and methods, Weiger-Weed produces offset welding electrodes with these important advantages: . . .

- No loss of physical or electrical properties—cold formed offset tips have the same hardness and conductivity as straight tips.
- No distortion of taper—good electrical contact, no water leaks.
- Full cooling—water hole is as deep as in straight tips.
- Full workable nose length.
- Economy, quick delivery—most styles are formed quickly from standard straight tips.

Many other special cold formed electrodes to lick difficult clearance problems can be worked out by WW if quantities warrant the tooling. Give us the details of your problem.

WW

22801A

WEIGER-WEED & CO., Division of Fentsel Metallurgical Corp. • 11644 Cloverdale Avenue • Detroit 4, Michigan

PERSONNEL

J. H. Humberstone Elected a Vice-President

Joseph H. Humberstone has been elected a vice-president of Air Reduction Co., Inc. He was formerly the President of the

Company's Airco Equipment Mfg. Division and he has been succeeded in that capacity by Scott D. Baumer.

OBITUARY

Robert J. Kehl

Robert J. Kehl, Consulting Engineer in the Development Dept. of Linde Air Products Co., died at his home on November 23rd. He was 62 years of age.

Mr. Kehl was born in Chicago in 1889, and received his early education there. He was graduated from Cornell University in 1912, and received his degree in mechanical engineering.

As Consulting Engineer, Mr. Kehl was primarily concerned with the engineering, design and safety features of all oxyacetylene equipment. He worked closely with many regulatory bodies, including Underwriters' Laboratories, Inc., and Factory Mutual Laboratories. He was also an authority on acetylene generators, and was noted for his achievements in the development of all types of apparatus used with the oxyacetylene flame. He developed the "balance seal" used on acetylene generators, and was widely recognized for his developments in the glass industry, notably the "safe-edging" of glass tumblers.

In 1913 Mr. Kehl joined the engineering staff of the Chicago plant of Oxweld Acetylene Co., now part of Linde Air Products Co. For many years he was active in the Engineering Dept. of the Company's Newark factory, where he later became Chief Engineer of Oxweld Acetylene Co.

In 1928 Mr. Kehl moved to Union Carbide's General Offices in New York, as a member of the Gas Group Engineering Dept. In 1933 he was promoted to the position of Consulting Engineer, and retained that position until his death.

Mr. Kehl was the author of the two textbooks, *Oxy-Acetylene Welding* and *Oxy-Acetylene Welding Practice*. He wrote many technical articles which appeared in trade publications.

Mr. Kehl was a member of the American Welding Society, American Society of Mechanical Engineers and International Acetylene Assn. He also was a member of the Ancient Arabic Order Nobles of the Mystic Shrine.

Kr. Kehl is survived by his wife, Amalie L. Kehl; a son, Roy E.; and a daughter, Joy Anna.

Black Binder Found

A small black binder with alphabetical index and some notes in it was found during the AWS Annual Meeting at the Book Cadillac Hotel, Detroit, the week of October 14, 1951. Contact the Editor, The Welding Journal, 29 West 39th Street, New York 18, N. Y.

WAGNER
ELECTRODE HOLDERS

The perfect teammate for WAGNER GROUND CLAMPS

RESIST ARC HEAT
Copperhead Tip Insulation operate near 6,000° heat.

COOLER
Patented channel construction flushes heat radiation twice as fast.

FULL CURRENT DELIVERED
Wagnerloy Construction results in high conductivity, longer service.

WAGNER MFG. CO.
350 W. 1st SOUTH ST.
JACKSON, MISSOURI

Weld with Wagner

WELDING RESEARCH COUNCIL

of the Engineering Foundation

Sponsored by the American Welding Society and American Institute of Electrical Engineers American Institute of Mining and Metallurgical Engineers, American Society of Civil Engineers American Society of Mechanical Engineers, and Society of Naval Architects and Marine Engineers

Supplement to the Welding Journal, February, 1952

The Ship Structure Committee

by Rear Admiral K. K. Cowart

THIS is the sixth year that the Ship Structure Committee has been privileged to sponsor technical sessions at the AMERICAN WELDING SOCIETY'S Annual Meeting. As Chairman of the Ship Structure Committee, I am pleased to have this opportunity of opening the first session this year with a few remarks about our Committee and its work.

Our association with the Welding Society has done much to facilitate the work of the Committee. The AMERICAN WELDING SOCIETY is preeminent in its field. The welding industry looks to the Welding Society for leadership to develop further the art and science of welding. The SOCIETY has well justified this position of trust. There is no question that the advanced position of welding in the United States today is due to a great extent to the progressive leadership afforded by the AMERICAN WELDING SOCIETY.

The WELDING SOCIETY has always maintained close cooperation with the Ship Structure Committee. Mr. Spraragen, Director of the Welding Research Council and Liaison Representative to the Ship Structure Committee, has done much to further this feeling of unity in our quest for knowledge in the closely related fields of welding and ship structure. He is highly regarded by those of us on the Committee.

The close relationship between the Ship Structure Committee and the AMERICAN WELDING SOCIETY has not come about by chance. Our interests are parallel in many respects. It is not possible to think of any aspect of modern ship construction without taking into account the vital part played by welding. Conversely, I am sure you will agree, modern ship structure comprises an important portion of the field of interest of the AMERICAN WELDING SOCIETY.

Although the mission of the Ship Structure Committee is obviously concerned with the structures of ships, it naturally follows that the inherent problems are basically the same as those occurring in other types of steel construction. Accordingly, many of the findings of our Ship Structure Committee apply equally to steel fabrication in all its forms.

The Ship Structure Committee and its predecessor, the Board to Investigate the Design and Methods of Construction of Welded Steel Merchant Vessels, have been devoted to a continuing research effort for almost nine years. As you know, the Board was convened by the Secretary of the Navy during the early part of World War II as a result of the large number of failures occurring in our emergency merchant fleet. Property loss and jeopardy to life seriously imperiled the vital logistic mission of our Merchant Marine. The ship failures of which I speak were not due to enemy action, grounding or collision. They occurred without warning under normal operating conditions, often resulting in complete loss of the ship.

At the outset it was generally believed that the welding process was the primary and sole cause of these ship failures. However, the Board of Investigation concluded that locked-in stresses do not materially contribute to the brittle fractures in welded ships. Instead of welding per se being the culprit it was found that the simple notch, sometimes present in imperfect welds, coupled with the characteristics of ship steels, is of more direct concern to the problem. A proper weld does not contain notches and hence does not contribute toward brittle cleavage fractures. Some notches which have resulted in ship failures have been inadvertently designed into the ship. The most notable design notch was the square hatch corner as installed in the early Liberty ships. The welding process cannot be held to account for the failures

Rear Admiral K. K. Cowart, Engineer-in-Chief of the U.S. Coast Guard, is Chairman of the Ship Structure Committee.

Paper was presented at the Thirty-Second Annual Meeting, AWS, Detroit, Mich., week of October 15, 1951.

which were traced to these design notches. Brittle fractures have occurred in riveted ships also. The essential difference is that in riveted ships cracks generally stop at a rivet hole or at a riveted seam.

Although the Board was soon aware of the importance of the "notch-sensitivity" or "notch-brittleness" of ship steel, there was no time in which to pursue the course of fundamental research which appeared requisite to the final solution of the problem. Ships were breaking up. The war effort was being threatened by forces even more elusive than any put forth by the enemy. Ships were being lost before they had even encountered the enemy. Time meant lives. Action must be bold. Immediate solutions must be found.

The Board was equal to its task. The action of the Board resulted in sharply reducing the incidence of ship failures to within resonable limits. But what were considered reasonable limits under war conditions could not be accepted as reasonable after hostilities ceased. Furthermore, many of the Board's recommendations were clearly intended as interim measures. The basic problem had not been solved. The Board therefore recommended that a group be formed for continuing long range search for a solution to the problem.

In view of the Coast Guard's statutory responsibility for the safety of life at sea, the Engineer-in-Chief was designated Chairman of the Ship Structure Committee. In addition to the Engineer-in-Chief of the Coast Guard, the membership of the Committee includes the Assistant Chief of the Bureau of Ships, USN; the Director, Merchant Marine Division of the Military Sea Transportation Service, USN; the Chief, Division of Construction and Repair of the U.S. Maritime Administration, and the Vice-President-Chief Surveyor of the American Bureau of Shipping. All of the five member agencies afford active technical and financial support to the Committee. Although the Transportation Corps of the Army no longer has membership on the Committee, that office does maintain liaison with the work of this group.

In his convening letter the Secretary of the Treasury charged the Ship Structure Committee with "prosecuting a research program to improve the hull structures of ships by an extension of knowledge pertaining to design, materials and methods of fabrication." These three main headings, Ship Design, Ship Materials and Ship Fabrication, are being explored in considerable detail.

In the field of Design, details of ship structure have been known to be focal points for cracking and are being studied. Several recommendations have already been made for the improvement of ship design details.

In the field of Materials, continuing effort is being made to establish a practical specification and test which will reliably identify those steels which have been found to cause trouble, i.e., the notch-sensitive steels. In addition, research projects are under way to improve the quality of shipbuilding steels with the view toward making them more notch-tough. Another phase of the Materials program is being directed toward the development of steels the composition of which will not include critical materials in time of war or national emergency. An important result of the Materials program is the accumulation of information on the performance of steel under cold weather conditions. Based on this information, hull steel requirements of the regulatory bodies have been improved.

Improvements have been recommended in shipyard fabrication methods with the view toward reducing the frequency of notches in the ship structure. Work in this field is continuing.

From its inception the Ship Structure Committee has directed its attention toward a more basic approach. During recent years it has been necessary to look even closer to fundamentals in searching for solutions to the elusive aspects remaining in our problem. An approach to pure scientific research has been necessary. As many of you are concerned with similar research endeavors, I am sure you will appreciate it when I say that we have found that the more basic our study has become, the more difficult it is to retain the objective view required to relate findings to the problem of brittle fracture of steel. It is possible that some of us lose sight of this tendency in spite of its obviousness. It is just another case of not seeing the forest for the trees. Recognizing this, we have found it necessary to give more time and attention to just plain thinking and evaluating and less to actual testing. This is especially true in ship structure work because of the diversified nature of the basic problem.

In order to retain a broad objective viewpoint and to obtain the advisory services of leaders in related fields of metallurgy and engineering, we have found the services of the National Research Council of the National Academy of Sciences to be invaluable. Under a Ship Structure Committee financed contract, the Academy Council has formed two committees to assist in our work. These two groups, the Committee on Ship Steel and the Committee on Residual Stresses, are doing much toward solving many phases of our problem. These well balanced and integrated organizations have made it possible for the Ship Structure Committee to spend more time on the interpretation of results.

In addition, the Ship Structure Committee is fortunate in enjoying the close cooperation of the American Iron and Steel Institute, the British Admiralty Ship Welding Committee, the Society of Naval Architects and Marine Engineers, the National Bureau of Standards, the Metallurgical Advisory Board of the National Research Council, as well as the American Welding Society. The Ship Structure Committee also utilizes many of the nation's foremost research agencies for the performance of research testing services. The technical papers which you will hear at the two Ship Structure Sessions here today will report on some of this work which has been financed by the Ship Structure Committee.

In spite of the advances made by the Ship Structure Committee toward improving the structures of ships, the final answer is not yet clear. Much remains to be learned. Ships still fail without warning. We are forced to cling to one of the emergency remedies designed during the war period. I refer to the riveted crack arrestor. As you know, the crack arrestor is simply a longitudinal riveted seam installed in the strength deck, bottom or side of a steel ship and is intended to stop the propagation of a crack. The discontinuity of steel across a riveted seam has been found to inhibit the travel of a fracture. Of course, the crack arrestor does not prevent fractures occurring in ships. It is intended only to limit the extent of travel across the ship's strength member. The crack arrestor has served to materially reduce the extent of fracture. But we do not consider the riveted crack arrestor as a proper solution to this problem. It is recognized by all as only an interim remedy.

With the crack arrestor as a symbol of our ignorance, the Ship Structure Committee is actively continuing its fundamental research program to improve the hull structure of ships. Many important unknowns still remain. The advantages of welding are recognized by all. Our effort must continue unabated.

The construction of all our ships should eventually use the welding process and yet must be capable of operating anywhere in the world without danger of destruction or injury in the form of hull fractures. The advantages of welding over riveting are so great that obstacles must be removed. Until we have solved those obstacles it will not be possible for us to construct an expanded merchant fleet under emergency conditions that will be completely safe against brittle fractures. Our ships must be all welded. We will overcome the problems which this involves. This can only come about, however, through a concentrated and coordinated program of research.

In conclusion I wish to broaden the scope of the words of that eminent British Director of Naval Construction, Sir Charles Lillicrap. We must all look forward to the day when steel structures of all kinds will be entirely "innocent" of rivets. To this end I suggest that the AMERICAN WELDING SOCIETY, along with the Ship Structure Committee, adopt this as a prime objective.

Welded Reinforcement of Openings in Structural Steel Members

Discussion by Wiley E. Magee

It appears that this program, even in the most direct form obtainable in reaching the desired objective, involves a considerable number of inherent complexities and cross currents, and that extreme care must be exercised to eliminate unnecessary complications.

As I see it, the desired objective stated in broad terms is to determine the underlying factors involved in designing a hatch corner which will provide, in so far as practicable, optimum performance under all operating temperatures. The term "optimum performance" is subject to interpretation and may eventually be based on such features as ultimate load, energy absorption, total elongation, etc., or a compromise interrelationship of such items. This is one side of the situation and concerns itself with the records taken; the other side concerns itself with sufficient variations in design to achieve, within practical limits, the maximum values of energy absorption, elongation, etc. Relative to design variations in thin plate work, the prime lesson already learned from low temperature heavy plate work should be kept in mind, i.e., eliminate all sharp notches, use generous radii in way of changing sections, and disperse changing sections.

The work already laid out and performed, I believe, is entirely adequate in some respects and in other respects I am not so sure. I feel that studies involving thick plates and low temperature testing probably should not be considered until design variations have been tested further in thin plate in an effort to determine the maximum practical attainable values of energy absorption, elongation, etc., due to the use of reinforcement material. In this work, one corner radius (say, $r = {}^1/_8$ the transverse dimension of the opening) should be sufficient for preliminary exploration purposes. This seems reasonable on the basis that the chosen opening and corner radius determines a specific biaxial field of stress for which the intent is to determine the fundamental principles governing effective distribution of reinforcing material. If such principles can be determined, there seems to be little barrier to applying them to the biaxial field determined by a variation in the corner radius, to achieve similar results. It is not known in this proposal what "effective" stress or strain consideration influences initial fracture and how distribution and redistribution of material at critical

(*Continued on page 77-s*)

Wiley E. Magee is Naval Architect, Merchant Marine Technical Division, U. S. Coast Guard; Alternate member Ship Structure sub-committee.

Paper by D. Vasarhelyi and R. A. Hechtman published in the Research Supplement to the April 1951 issue of THE WELDING JOURNAL.

The views and opinions herein expressed are strictly those of the discussor and are not to be construed as representative of the organizations with which the discussor is associated.

Stress Studies of Bulkhead Intersections for Welded Tankers

A study of interrupted and bulkhead intersections for welded tankers to determine the magnitude of stress concentrations and areas affected by discontinuities

by Wm. R. Campbell, L. K. Irwin and R. C. Duncan

Abstract

Four large bulkhead intersection specimens were tested in tension in the Engineering Mechanics Section of the National Bureau of Standards for the Ship Structure Committee. The tests are part of a study of the typical structural discontinuities in ships to determine the magnitude of stress concentrations and areas affected by discontinuities, and to furnish data necessary for improving current designs. Each specimen represents the intersection of a longitudinal bulkhead and a transverse bulkhead as found in welded tanker design. The elastic stress distribution at room temperature, strain distribution prior to failure at 0° F. and energy for failure of 0° F. are presented. Stress values in the region of the intersection are compared with stresses in the longitudinal bulkhead bordering the intersection. Stress concentrations and energy at failure for the different specimens are also compared.

INTRODUCTION

THE present paper is the second of a series reporting the results of ship structure tests performed at the National Bureau of Standards as part of a general study of structural discontinuities sponsored by the Ship Structure Committee. A program for the study of interrupted longitudinals and bulkhead intersections for welded tankers was developed by a Project Advisory Committee with representatives from the member agencies of the Ship Structure Committee. The purpose of the test program is to determine the magnitude of stress concentrations and areas affected by discontinuities, and to furnish data for improving current designs.

The results of tensile tests on four interrupted bottom longitudinals have been previously reported.[1] The present paper is concerned with tensile tests of four bulkhead intersection specimens. Elastic tests at room temperature and tests to failure at 0° F. were made on each specimen. The elastic stress distributions were determined by loading the specimen and measuring the strains on sufficient gage lines to determine the state of strain from which the state of stress could be determined. Tests to failure on each specimen were conducted at 0° F. in an effort to duplicate the brittle fractures which have been characteristic of many welded ship failures. From these tests, strain distribution and energy to failure determinations were made.

SPECIMENS

The corrugated-plate bulkhead intersection specimens represented full-sized sections at the connection of the longitudinal bulkhead and the transverse bulkhead. Isomeric representations of the four specimens, numbered 5 to 8, are shown in Fig. 1. The specimens were fabricated from steel ship plates joined by welding. Specimen 5 represented the basic structure, modifications of which were used in fabricating specimens 6, 7 and 8. Full penetration welds at the longitudinal bulkhead connection on Specimen 6 replaced the fillet welds used on Specimen 5. At the longitudinal bulkhead connection and in line with the tee web, brackets were introduced on both sides of the sloping corrugated section on Specimen 7. Specimen 8 was obtained by adding a formed angle at the longitudinal bulkhead connection.

The tensile properties of the ship plate, Table 1, were determined on samples taken from the plate stock before fabrication of the specimens.

TESTING PROCEDURE

The bulkhead intersection specimens were loaded in a horizontal Emery testing machine having a capacity in tension of 1,150,000 lb. Each specimen was placed in the testing machine with the longitudinal bulkhead horizontal. Specimen 5 was the first specimen tested, and the loads for this specimen were applied through two U bolts at each end of the

Table 1—Mechanical Properties of Ship Plate at Room Temperature

	Sample				
	A	*B*	*C*	*D*	*Av.*
Thickness, in.	0.500	0.500	0.500	0.500	0.500
Young's modulus, psi. × 10^{-3}	30,100	29,900	30,500	30,100	30,100
Poisson's ratio	0.287	0.289	0.293	0.292	0.290
Proportional limit,* psi. × 10^{-3}	30.7	34.2	30.9	30.1	31.5
Yield point, psi. × 10^{-3}	33.7	35.6	36.8	36.8	35.7
Tensile strength, psi. × 10^{-3}	61.8	62.0	62.0	61.8	61.9
Elongation in 8 in., %	32.0	30.4	32.0	31.9	31.6

* 30×10^{-6} offset.

Wm. R. Campbell, L. K. Irwin and R. C. Duncan are associated with the National Bureau of Standards, Washington, D. C.

Paper was presented at the Thirty-Second Annual Meeting, AWS, Detroit, Mich., week of Oct. 14, 1951.

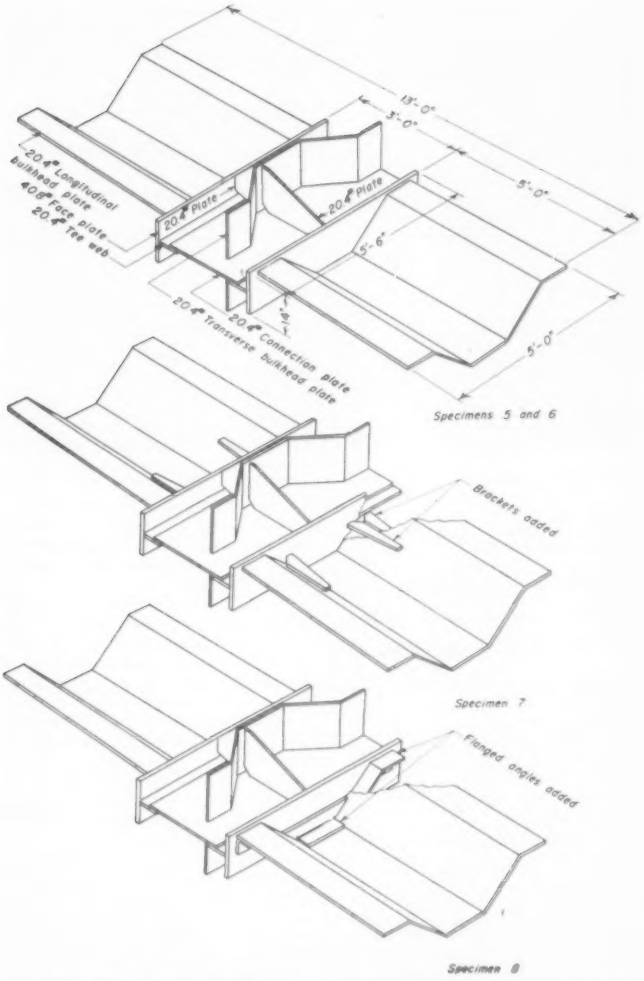


Fig. 1 Dimensions and details of bulkhead-intersection tensile specimens

Fig. 2 Laboratory setup for elastic tests on Specimen 6. Welded pulling tabs, pulling plates and eye fixture at right

specimen. The legs of the U bolts projected through four holes in plates welded to each end of the specimen. The throats of the U bolts encircled the legs of a T-shaped fixture, the stem of which was gripped in the head of the testing machine. Following the test of Specimen 5, two of the U bolts failed during the course of other tests, and the U bolt arrangement was subsequently discarded in favor of pin loading. The loads for specimens 6, 7 and 8 were applied through reinforced welded-plate headers which were connected by pins and straps to eyebars gripped in each head of the testing machine. Specimen 6 is shown in the testing machine in Fig. 2.

ELASTIC TESTS AT ROOM TEMPERATURE

SR-4 single-element resistance strain gages, Type A-3, and three element rosettes, Type AR-2, were installed on each specimen, the locations of those gages for specimen 8 being shown in Fig. 3. Each gage symbol in Fig. 3 represents two gages, one on each side of the specimen in similar locations, except for the edge gages. Gages were air dried for 16 hr., heated with infrared lamps for 3 hr. and waterproofed with a mixture of equal parts U.S.P. petrolatum and petrosene wax. Specimen 6 is shown in Fig. 4 with gages and wiring attached. Strain readings were taken on all gages at loads of 20, 60, 100, 140 and 160 kips using SR-4 portable strain indicators.

TESTS TO FAILURE AT 0° F.

Following the test at room temperature, an open-top box of insulating board was built around each specimen for the test at 0° F. Solid carbon dioxide was used as a coolant, and ten copper-constantan thermocouples were installed on each specimen, as shown in Fig. 3, for temperature determinations. Two over-all dial extensometers were attached to the headers for determining the center line extension of the specimen. The welds between the specimen and the end tabs were heated with infrared lamps during the test to reduce the possibility of failure of the end connections at low temperature. The low temperature test setup for specimen 7 is shown in Fig. 5 with the near side of the box removed.

With the temperature of the specimen near 0° F., strain readings were taken on all gage elements parallel to the axis of the specimen at load increments of 50 kips up to a load of 200 kips and thereafter at increments of 100 kips until failure was imminent, or until a number of important

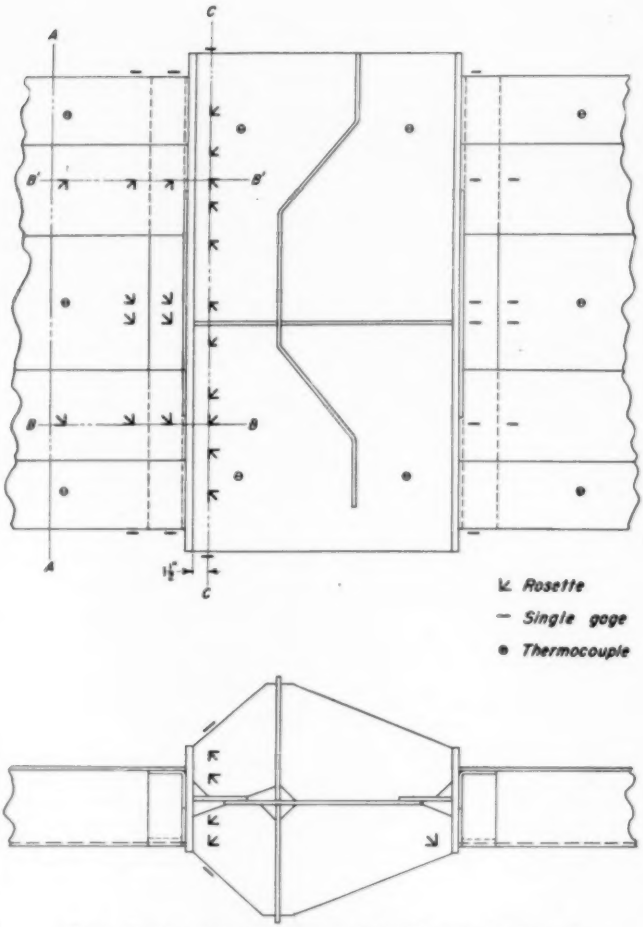


Fig. 3 Strain gage and thermocouple locations for Specimen 8

gages became inoperative. The dial extensometers were read up to the instant of failure.

Specimen 5, loaded with the U-bolt-type fixtures, was not tested to complete failure. The test was stopped when one of the smaller vertical brackets fractured.

STRESS DISTRIBUTION AT ROOM TEMPERATURE

The strains for corresponding gage lines on opposite sides of plates were averaged for each load. The load-average strain relationships were reasonably well represented by straight lines. Stresses due to a 160-kip load were computed from the linear load-strain relationships by standard methods.[2,3] The magnitude and direction of the principal stresses at each rosette were computed using the average values of Young's modulus and Poisson's ratio given for the material in Table 1. Axial stresses for determining the stress distribution on sections *B-B* and *C-C* of each specimen, Fig. 3, were computed from the principal strains for a load of 160 kips. Section *B-B* was in line with the intersection of the sloping sides of the longitudinal bulkhead and the tee web. The ratios of the axial stresses to the average stress in the longitudinal bulkhead outside the area of the intersection were computed to give a measure of stress concentrations. The average stress in the longitudinal bulkhead (P/A) was taken as the load, 160 kips, divided by the area of the unmodified portion of the longitudinal bulkhead, section *A-A*.

The magnitude and direction of the principal stresses for the four specimens are shown in Figs. 6 to 9. Comparison of the principal stresses and their directions indicates that similar stress patterns were obtained for the different specimens. Major principal stresses fan out with diminishing magnitudes from the intersections of the sloping sides of the longitudinal bulkhead and the web of the tee. Bracket stresses converged on the web of the tee. The highest stresses were measured at the intersection of the sloping sides of the longitudinal bulkhead and the tee web and at the intersections of the longitudinal bulkhead and the brackets. The exceptionally large stresses measured on the brackets of Specimen 7 are thought to be in part due to an initial misalignment of the component plates during fabrication.

The axial stress distributions on the tee web, section *C-C*, of specimens 6, 7 and 8 and peak stresses for Specimen 5 are given in Fig. 10. Axial stresses on the tee web reached maximum values at the intersection of the sloping sides of the longitudinal bulkhead. Specimens 7 and 8 with the structural modifications shown in Fig. 1 exhibited lower axial stresses at these points. The ratio of the stresses here to the average stress in the longitudinal bulkhead ranged from 2.1 for Specimen 7 to 3.4 for Specimen 6.

The axial stresses at corresponding gage locations on sections *B-B* and *B'-B'* were averaged for each specimen. These average axial stresses are shown in Fig. 11. The stress distributions for specimens 5 and 6 were generally alike but were markedly different from the distributions for modified specimens 7 and 8. The stresses at this section were significantly lower on specimens 7 and 8 than on specimens 5 and 6. Stresses in the longitudinal bulkhead 16 to 20 in. from the face plate and in line

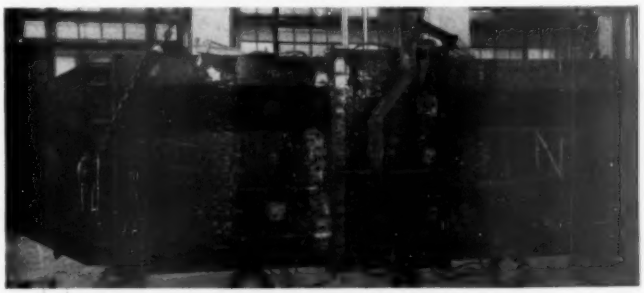


Fig. 4 Specimen 6 with strain gages and wiring attached

Fig. 5 Specimen 7 in testing machine for test at 0° F. Near side of box removed. Long-gage extensometers at top and to right

with the intersection of the sloping side and the tee web were 1.2 to 1.7 times the average stress in the longitudinal bulkhead.

Excluding specimen 5, the average axial stresses on the tee webs and brackets were computed and multiplied by the areas of the web and brackets respectively to obtain the loads carried by these components. The loads carried by the tee webs and brackets at Section *C-C* are given in Table 2. The sum of the computed web and bracket loads differed from the testing-machine load of 160 kips by 2.0 to 12.7%. It is probable that the computed tee-web loads are more accurate than the computed bracket loads, since the stress distribution on the tee web is more easily defined by the limited number of stress values. Accordingly, it is also probable that bracket loads obtained by taking differences between the testing-machine load and the computed tee web (Table 2) loads are more reliable than the computed bracket loads.

STRAIN DISTRIBUTION PRIOR TO FAILURE AT 0° F.

The axial strains for gages on opposite sides of the plate in similar locations were average for each load. Strains at corresponding gage locations for sections *B-B* and *B'-B'* were averaged. Strain distributions for sections *B-B* and *C-C* at a load of 700 kips are shown in Figs. 12 and 13 for specimens 6, 7 and 8.

The strain distributions at high loads generally followed the pattern of the elastic stress distributions, except that Specimen 8 showed a significant reduction in stress concentrations at high loads. Comparison of Fig. 12 with Fig. 10 shows specimens 7 and 8 to be interchanged with regard to magnitudes of stresses and strains. At points of high stress specimens 7 and 8 were strained considerably less than Specimen 6.

ENERGY TO FAILURE AT 0° F.

Comparisons between the energies required for failure of the different specimens are difficult to make. On all specimens, brackets fractured at loads less than the maximum load, causing discontinuities in the elongation measurements.

Curves of center line extension versus load are given in Fig. 14. The energy to failure for each specimen was computed as the area under the load-extension curve, the extension being measured between the upright posts shown in Fig. 5. The computed energies are given in Table 3.

MAXIMUM LOADS

The maximum loads sustained by the four specimens are given in Table 3. All the specimens failed with brittle fractures except specimen 5 for which the failure was incomplete. Specimens 6 and 7 fractured across the longitudinal bulkhead near or at the connection with the face plate of the tee. The fracture of Specimen 6 occurred partially in the weld metal of one of the sloping sides. After the test an inclusion approximately three inches long was observed in the weld metal at the intersection of one sloping side and the web of the tee. Specimen 7 fractured in the parent plate except for one of the brackets in line with the tee web. This bracket fractured in the weld metal at the face plate. The fracture of Specimen 8 was across the tee web in the parent plate.

Table 2—Measurements and Results of Elastic Tests

	Specimen No.			
	5	*6*	*7*	*8*
Testing machine load, P, lb. $\times 10^{-3}$	160	160	160	160
Section *A-A*				
Area of section, in.^{2}	33.60	32.88	34.81	33.23
Average stress,* psi. $\times 10^{-3}$	4.8	4.9	4.6	4.8
Maximum stress 16 in. from face plate, psi. $\times 10^{-3}$	6.4	7.2	8.5†	6.2
Section *C-C*				
Area of section, in.^{2}	...	40.49	41.05	40.82
Area of tee web, in.^{2}	...	32.7	33.3	32.9
Maximum tee web stress, psi. $\times 10^{-3}$	14.9	16.6	9.9	13.9
Average tee web stress, psi. $\times 10^{-3}$	...	4.2	3.5	4.2
Tee web load, P_t, lb. $\times 10^{-3}$	...	137.3	117.9	138.2
Area of brackets, in.^{2}	...	7.76	7.78	7.89
Maximum bracket stress, psi. $\times 10^{-3}$	5.1	11.2	29.9	9.9
Average bracket stress, psi. $\times 10^{-3}$	...	3.8	8.0	3.2
Bracket load, P_b, lb. $\times 10^{-3}$	...	29.5	62.4	25.0
Bracket load, P-P_t, lb. $\times 10^{-3}$	...	22.7	42.1	21.8

* Stresses given are for the axial direction.
† Stress was measured 20 in. from face plate on Specimen 7.

Table 3—Results of Low Temperature Tests

	Specimen No.			
	5	*6*	*7*	*8*
Maximum load, lb. $\times 10^{-3}$	860*	774	944	1,129
Energy to failure, ft.-lb.	11,400*	61,800	45,190	68,800
Average stress on section *A-A* at maximum load, psi. $\times 10^{-3}$	25.6*	23.5	27.1	34.0

*Partial failure of Specimen 5.

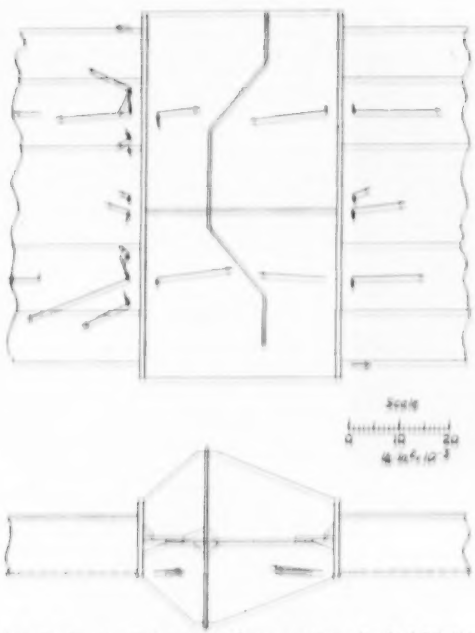


Fig. 6 *Specimen 5, principal stresses for a load of 160,000 lb. Principal stresses less than 1000 psi. not shown*

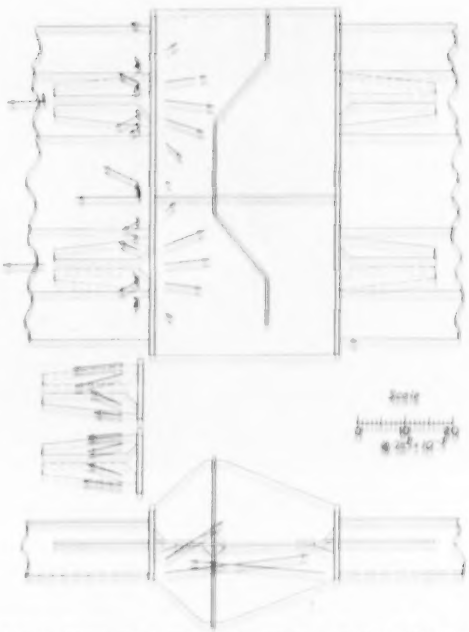


Fig. 6 *Specimen 7, principal stresses for a load of 160,000 lb. Principal stresses less than 1000 psi. not shown*

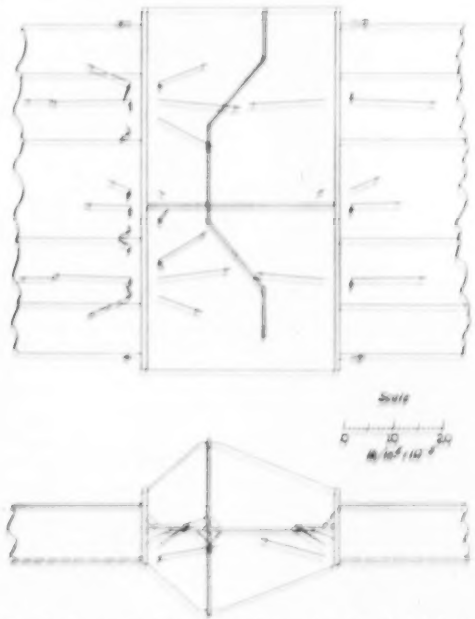


Fig. 7 *Specimen 6, principal stresses for a load of 160,000 lb. Principal stresses less than 1000 psi. not shown*

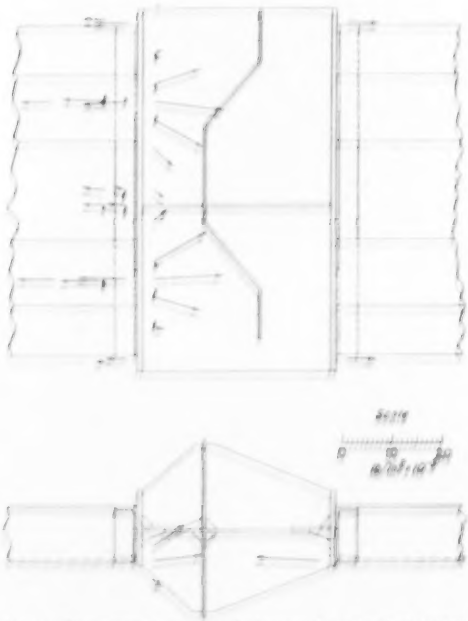


Fig. 8 *Specimen 8, principal stresses for a load of 160,000 lb. Principal stresses less than 1000 psi. not shown*

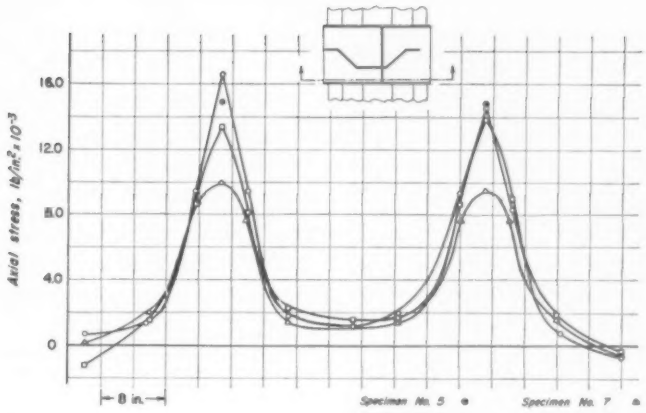


Fig. 10 Distribution of axial stresses on tee web, Section C-C. Load 160,000 lb.

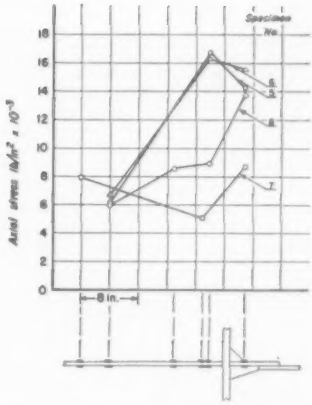


Fig. 11 Distribution of axial stresses on Section B-B. Load 160,000 lb.

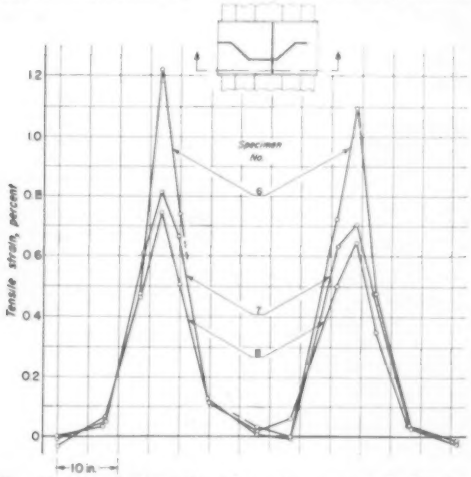


Fig. 12 Distribution of axial strains on tee web, Section C-C. Load 700,000 lb.

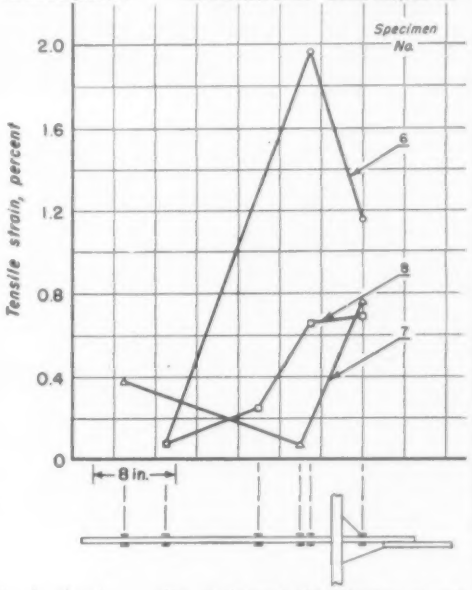


Fig. 13 (above) Distribution of axia strains on Section B-B. Load 700,000 lb.

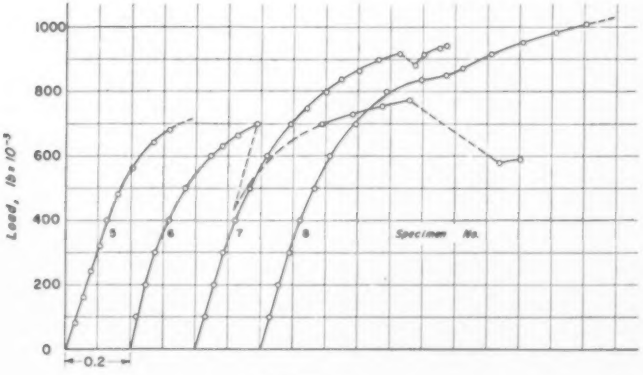


Fig. 14 Load vs. center line extension

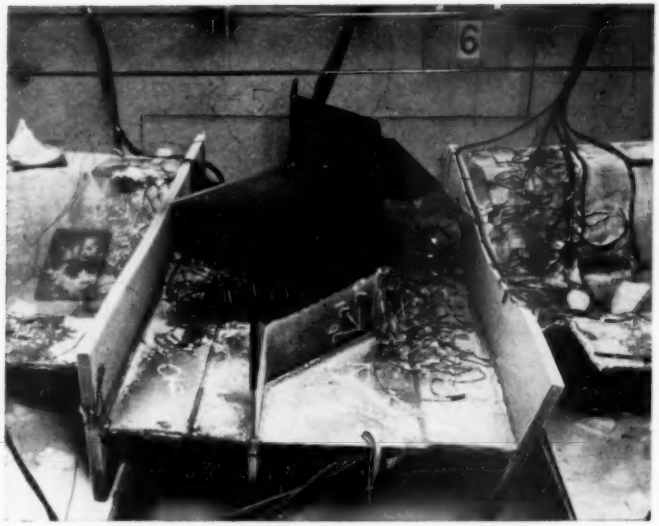


Fig. 15 Specimen 6 after test at 0° F.

Chevron patterns on the fracture surfaces of specimens 6, 7 and 8 indicated that the fractures started at the intersection of the sloping bulkhead section and the web of the tee. Specimens 6, 7 and 8 are shown after failure in Figs. 15 to 17.

The average stresses (P/A) on the longitudinal bulkhead section A-A at the maximum load were computed for each specimen and are given in Table 3. These stresses were below the tensile yield strength of the material (Table 1) for all specimens except Specimen 8.

The efficiencies of the four specimens were computed for several values of load. The efficiency, e, was taken as the ratio

$$e = \frac{\partial a}{\partial c} 100 \qquad (1)$$

where ∂a is the average stress (P/A) on section A-A through the longitudinal bulkhead at load P, and ∂c is the stress required to strain an axially loaded bar of the same material and area to the average over-all strain recorded for the specimen at the load P. Efficiency versus load curves for the four specimens are shown in Fig. 18. Figure 18 indicates that all modifications of the basic structure, Specimen 5, increased the efficiency of the structure for elastic loading. It is interesting to note that the greatest increase in axial rigidity for low loads was affected by the substitution of full-penetration welds for double-fillet welds on the longitudinal bulkhead on Specimen 6. The change in welds had no beneficial effect on stress concentrations, however.

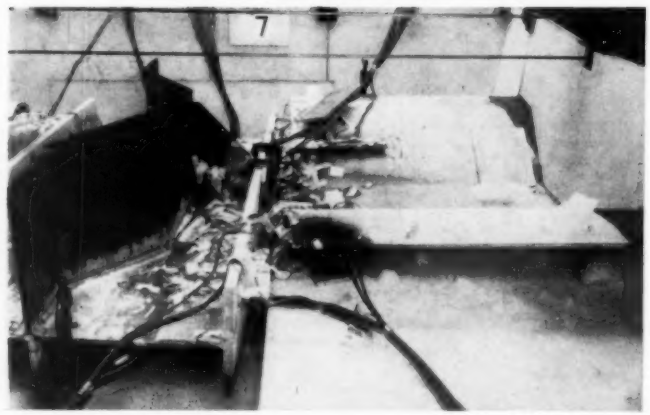


Fig. 16 Specimen 7 after test at 0° F.

SUMMARY

The elastic stress distribution at room temperature, strain distribution prior to failure at 0° F., and energy to failure at 0° F. were determined for four bulkhead-intersection specimens, and the maximum loads were measured. Stresses in the axial direction ranging from 2.1 to 3.4 times the average stress in the longitudinal bulkhead were measured at the intersection of the tee web and the sloping sides of the longitudinal bulkhead. The strain distributions for high loads did not differ appreciably from the elastic stress distributions. The four specimens sustained considerable elongation before failure, but all specimens ultimately developed brittle fractures in the area of the intersection.

Based on the energy for failure, maximum load and reduction of stress concentrations at high loads, Specimen 8, for which the basic structure was modified by the addition of angles at the bulkhead connection, showed the most promising performance of the four designs studied.

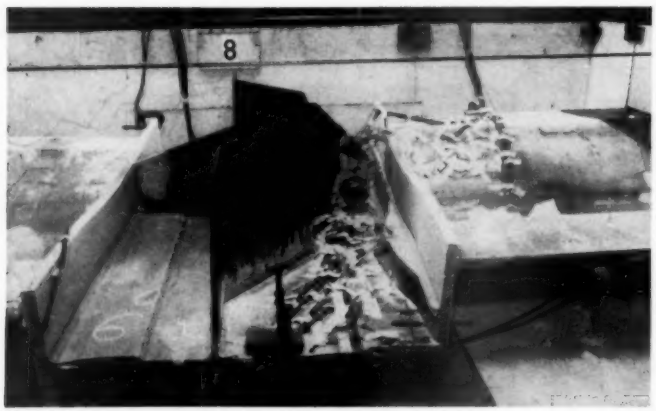


Fig. 17 Specimen 8 after test at 0° F.

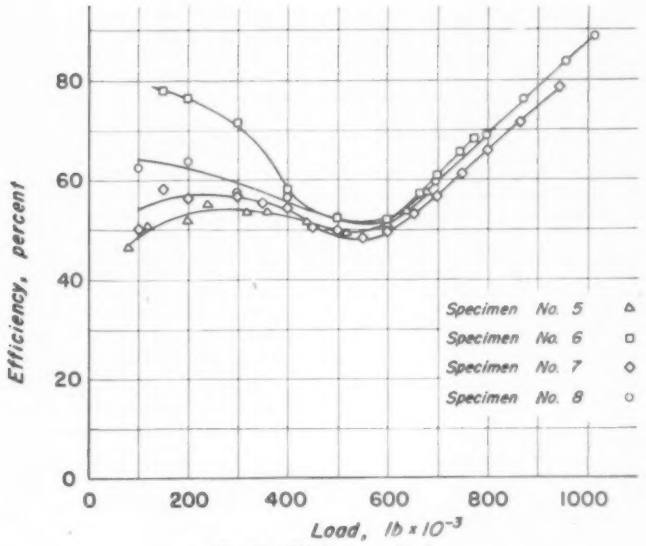


Fig. 18 Efficiency vs. load

Based on reduction of stress concentrations under elastic conditions, Specimen 7 showed the best performance.

ACKNOWLEDGMENTS

Acknowledgment is made to the Ship Structure Committee and the Bureau of Ships without whose program of research and financial assistance these tests would not have been undertaken. During the course of these tests many valuable suggestions were received from the Project Advisory Committee, consisting of Cmdr. James McIntosh, USCG, *Chairman*, E. M. MacCutcheon, Jr., W. G. Frederick, John Vasta and Mathew J. Letich, and from Dr. Finn Jonassen, technical adviser to the Ship Structure Committee.

References

1. Campbell, Wm. R., "Stress Studies of Welded Ship Structure Specimens," THE WELDING JOURNAL, 30 (2), Research Suppl., 68-s to 76-s (1951).
2. Baumberger, R., and Hines, F., "Practical Reduction Formulas for Use on Bonded Wire Strain Gages in Two-Dimensional Stress Fields," *Experimental Stress Analysis*, Vol. II, No. 1 (1944).
3. Timoshenko, S., *Strength of Materials*, Part I, D. Van Nostrand Co., New York, pp. 44–52 (June, 1940).

Discussion by Captain Wendell P. Roop, USN (Retired)

The part of a ship here tested presents a problem in load transfer from a tensile member to its continuation through an intersecting member. The older solutions of this problem found for floors and longitudinals in a double bottom are not available because both of the intersecting members have corrugated sections.

The present solution lies in substituting a flat plate for the corrugated plate of the longitudinal member at the intersection. The resulting geometry is like that of a round tube joined end-on to a square one; the load must be carried through at a number of oblique crossings of the mismatched tube walls. The transfer is helped by interposing a heavy face plate which spreads the load at the crossings. The thicker the face plate and the more numerous and oblique the crossings the greater the mitigation.

But this only partly relieves the difficulty. It is one thing to increase the area of contact of metal, coming as near as possible to the full area of the main tension members, but it is equally necessary and more difficult to achieve a balanced structure which eliminates eccentricity and transverse reactions and avoids troublesome hard spots of concentration and constraint.

All of the models reached overall elongations exceeding elastic limits before even partial rupture. Thus average over-all strain at first rupture was about 0.25% in No. 6, 0.4% in No. 7, and 0.6% in No. 8. These numbers reflect the gross capacity for absorbing energy although maximum loads also were higher on Nos. 7 and 8 than on No. 6. These were thus better than No. 6 in capacity for both load and elongation *before* partial rupture. No. 6 did better *after* than *before* partial rupture, but in service under sustained load partial rupture would not have afforded relief and complete rupture would have followed. The average strains quoted are to be compared with the value of 0.1% at the proportional limit of the material.

At 700 kips load (stress 20 ksi.) the average over-all strain in the three models was 0.25%, 0.19%, 0.19%. At and below this load level Nos. 7 and 8 were therefore more rigid than No. 6 and showed elongations only about double those which would occur at the proportional limit of the material in a uniform bar, about 1000 kips.

The base on which overall elongations were measured seems to be about 160 inches, so that a uniform bar would stretch elastically up to 0.16 inch. Instead of this, models 5, 7 and 8 stretch from the start at about double this rate and reach a proportional limit at 300 to 400 kips instead of 1000 kips.

Only in No. 8 is final rupture deferred until load approaches the elastic proportional limit. This failure of the load, even in the best model, to exceed the elastic proportional limit before complete rupture, combined with the other facts cited, shows that serious concentrations exist. These increase the rate of elongation, even at the lowest load; the P.L. of the material must therefore already have been exceeded locally in the models at less than $^1/_{10}$ the load which would have the same effect in a uniform bar. By the time this fraction has reached $^1/_2$, the concentrations have become so widespread as to cause the over-all elongation to increase even more rapidly.

An exception is noted in No. 6 which at the very beginning elongates only at about the elastic rate. This effect is nonlinear, however, even from the start, and elongation soon increases faster than in any of the other models. Further comment on No. 6 is made later.

The over-all absorption of energy, taken to rupture, even in the best case, is very low, 160 in.-lbs. per cubic inch. In the Swarthmore 12 by $^3/_4$ inch plates, with machined notches of high acuity, this quantity seldom dropped below 400 to 500 in brittle fracture, and in ductile specimens is of the order of 5000.

Energy at partial rupture in Nos. 5 and 6 is only about 20 in.-lbs. per cubic inch. This may be significant when linked with an anomaly shown in Figs. 11 and 13;

in a location in the same region as that in which final rupture occurred, stress intensity and strain value are 2 to 3 times as high as in No. 8. The brackets may account for this; they are a small price to pay for an 8-fold increase in energy absorption.

However the low energy absorption, even in No. 8, as compared with the Swarthmore plates, indicates that a large improvement is still possible.

No mention is made in the report of constraint. Transverse components of stress, though present, are small. There are notable departures of the principal stress from the axial direction, but not enough to affect the distribution curves of Fig. 10 very much.

The plastic flow which occurred between the loads of 160 kips as in Fig. 10 and 700 kips as in Fig. 12 was not enough to soften the hard peaks or equalize them as between the different models. The minor changes in design reduced these peaks somewhat but, at best, concentration factors of at least 2 still show in Fig. 10, and at points in the metal closer to the hard spots must run much higher.

Some comment may be made on the part of the material in these tests. However transition temperature may be defined, the structure was brittle with respect to its capacity to smooth out hard spots by plastic flow. No doubt the fractures were all square rather than oblique and had the texture associated with brittleness. Nevertheless there was enough plastic deformation to increase over-all elongation as already noted. No data on notch toughness are reported, but the "lower transition" ("ductility transition," Lehigh T_D) must be below the temperature of these tests.

Since the temperature of the test lies between the two transitions of the material, and since the performance of this structure at this temperature was not satisfactory, it is the upper and not the lower transition that is significant.

Does a transition temperature exist for a given structure made of a given material? If so, what is its relation to that of the material? It is my own belief that it depends also on its design.

To use less cryptic expressions, I believe that a design of a structure for this same purpose and of this same material can be found, such that at this same temperature a substantial increase in energy absorption prior to rupture could be had. That is the direction to go for improvement in service behavior.

Discussion by James B. Robertson, Jr.

I am very glad to have this opportunity to comment on this paper by Messrs. Campbell, Irwin, and Duncan as it is my belief that they do very good work. Nevertheless there are a number of criticisms I should like to offer in the hope that either they may be discredited in rebuttal or may prove of use in the further improvement of future work. Some of these criticisms are not applicable only to this paper but are also applicable to other papers which have been presented in the field of structural investigation lately.

1. Due to the space limitations in the use of strain gages, it is believed that they can be considered effective for a critical determination of the state of stress only for details having a comparatively gradual change of section. In the designs reported on in this paper a very abrupt change in section occurs at the flange of the Tee web section but the rosettes along section C-C are $1^1/_2$ inches away from this critical location.

2. On page 6 the authors state "Comparison of Fig. 12 with Fig. 10 shows specimens 7 and 8 to be interchanged with regard to magnitudes of stresses and strains." It is believed that some further exposition at this point would assist naval architects like myself and others who may have a similar background in the understanding of this particular apparent anomaly regarding the relationship of stress and strain.

3. Since the 2 dial gages used to measure axial extension are located relatively close together on the same side of the specimen and relatively far from the axis, it is believed that the axial extension measurements cannot be too accurate. It appears that a much better arrangement for this purpose would consist of locating gages as close to 180 degrees apart as possible.

4. The use of a legend on each figure indicating the condition, etc., to which it applies would be most helpful. It would also be desirable if the grid pattern used had the same scale ratio for all related figures. In this paper 1 horizontal unit of the grid pattern equals 4 inches for Figs. 10, 11 and 13 but equals 5 inches for Fig. 12. While it is recognized that such a practice may not be practicable in a paper such as this having a brief text, it is considered generally desirable that figures and text be integrated into one whole rather than being presented in 2 sections.

James B. Robertson, Jr. is a Naval Architect Merchant Marine Technical Division. U. S. Coast Guard.

Discussion by John Vasta

This paper has given us many valuable results. It has shown that the specimens tested in the laboratory experienced appreciable elongation before failing in a cleavage mode. This is another example where structural elements are capable of sustaining moderate ductility prior to rupturing in what appears to be a brittle mode. Moreover, the tests have confirmed the service experience of the T-2 tankers with regard to the fractures sustained at the juncture of the corrugated bulkheads at the midpoint of the sloping face of the corrugation and at the interrupted longitudinals. Furthermore the results indicate that the remedial measures taken to correct the bulkhead intersection in the ships are beneficial in lowering the stress concentration factor, and in improving the load carrying capacity. It is most gratifying to note from this investigation, therefore, that it is possible to design and make laboratory tests of complicated structural assemblies whose elastic behavior is essentially similar to the one observed on actual ships.

Loading the test specimens to ultimate capacity makes these results alarmingly significant. In terms of average or nominal stress it is to be noted that even the best detail succeeded in carrying only an average stress not very much higher than the yield strength of the material. This should add a word of caution to designers for it means that the coupon strength of the material may not be realized in a complicated structure where the geometry introduces complex stress patterns and local restraints as to seriously impair the load carrying ability of the assembly.

John Vasta, Bureau of Ships, U.S.N.

Authors' Reply

The authors are indebted to Capt. Roop for a comprehensive summary of the many factors involved in the details of the nonsymmetrical intersections and in the evaluation of the test results.

The use of over-all strain as a measure of the rigidity of the different specimens has notable merit, and the comparison of average strains at particular loads is quite straightforward. The reference to the eightfold increase in energy absorption of Specimen 8 over specimens 5 and 6 should be considered with reservations, since during the test of Specimen 8 audible effects indicated that partial failure of this specimen may have also occurred, although not to an extent sufficient to cause discontinuities in the over-all elongation measurements. In any case, the low energy absorption of all the specimens together with pronounced stress concentrations point to the possibility of further improvements in design.

Mr. Robertson's comments on instrumentation are very much appreciated. With regard to the placement of strain gages, the location of rosettes on Section C-C was dictated largely by the nature of the specimen. Section C-C was the only section common to all specimens on which the primary effects of the different modifications could be compared. Although the gages were admittedly cramped, it is believed that at points $1^1/_2$ in from tee-flanges (3 plate thicknesses) the theory of plane strain does apply.

The statement that "comparison of figure 12 with figure 10 shows specimens 7 and 8 to be interchanged with regard to magnitudes of stresses and strains" was intended to call the reader's attention to the fact that, at points of greatest stress concentration, the rate of strain for elastic loading was less for Specimen 7 than for Specimen 8. At higher loads, however, the strain rate of Specimen 7 exceeded that of Specimen 8 at these same points.

The method of measuring over-all extension by the two-dial system is technically sound. Disregarding other factors it is agreed that some advantage would be had by locating the dials on opposite sides of the specimen, however, it is misleading to regard this as necessary to perfectly reliable measurements. It should be remembered that the primary quantity to be measured is extension. One dial would be sufficient, if it could be placed on the centroidal axis of the specimen. Since this was not possible, two dials were necessary in order to compute the center line extension by triangulation, thus correcting for any rotation of the dial post about its end. Whether the dials are on the same or opposite sides of the specimen, the essential requirement is that the dials be sufficiently far apart and sensitive to detect angular motion of the dial post. In these tests, by reading the dials to 0.001 in., rotations at the ends of the specimen could be determined within 0.01 degree, or about 40 sec. of arc.

It is extremely gratifying to learn from Mr. Vasta that the results of the present tests, which were obtained under laboratory conditions with a greatly simplified system of loading, show good correlation with shipboard measurements.

Welded Reinforcement of Openings

(*Continued from page 67-s*)

points can delay such fracture by inducing more generalized instead of local straining, but some preliminary data mentioned below has already been uncovered.

It is interesting to note that for most of the specimens with openings, the initial fracture started at a single point which involved all the following features:

1. Initial Luders lines development.
2. Maximum elastic strain concentration.
3. Principal strain concentration (presumably plastic strain).

The fractures which started elsewhere started at the outer boundary of the reinforcement and were described as "shear" failures.

The following are three out of a number of promising schemes of material distribution which may have interesting results:

1. Faceplate set back from edge of opening on both sides of plate. Weld connecting faceplate to test plate should lock compression in plate material at edge of opening.
2. Coamings, tapered at ends, running alongside edges of openings parallel to direction of load and extending past ends of opening.
3. Combination of faceplate and insert plate.

On the subject of energy absorption, I would like to raise the question as to which is the more revealing quality for purposes of analysis, the "energy-absorption to max. load (i.e., initial failure)" or "the energy absorption to final failure" as used in the report and involving tearing across the plate. I am inclined to the former.

Still on the subject of energy absorption or rather the elongation associated therewith, the 36-in. gage length employed in the tests to date appears to be questionable. It appears that the proper gage length should only cover the length of the plate where the influence of the opening causes the unit strains to depart from those at a distance from the opening. Upon this basis an 18- or 20-inch gage length may be productive of the most enlightening results. Of course with such an arrangement, it would be well to also record average unit strains at a distance from and unaffected by the opening.

The geometry of specimens as selected for the test series appears to involve a feature of questionable sig-

(*Continued on page 94-s*)

Ferrite-Grain-Size Measurements for Ship Plate Steel

Microstructure is an important element in the transition temperature of ship plates subjected to the heat of the welding cycle. Methods for determining ferrite grain size in ship steel are described and compared

by J. E. Campbell, R. H. Frazier and H. O. McIntire

Abstract

Ferrite grain sizes were determined in Class A ship plate in the as-rolled condition and after austenitizing at various temperatures followed by cooling at various cooling rates. Results obtained using intercept methods, grain counting, comparison with a special series of photomicrographs and a fracture method for rating the ferrite grain sizes were compared. Grain counting was the most reliable method and was used to show the effects of austenitizing temperature and cooling rate on grain size.

INTRODUCTION

IT HAS become apparent, from the work done in the last few years on the causes of brittle failure in ship plate, that a more precise means of determining ferrite grain size is needed. The ferrite grain size of $^3/_4$-in. ship plate steels usually falls within the range of 40 to 120 grains per square inch at 100 diameters. This range in grain size approximately covers the range for ASTM[1] Grain Sizes Nos. 7 and 8. Therefore, the use of ASTM numbers for designating grain size is not accurate enough to show the relationship between grain size and transition temperature. Methods which have been tried for determining ferrite grain size in ship steel are described and compared in this report.

The grain sizes of specially heat-treated ship plate steel were determined by use of the intercept method, by counting, by comparison with a special series of photomicrographs, and by fracturing after cooling in liquid nitrogen. The results suggest that the counting method gives the most reproducible results and the best agreement among investigators. However, it is somewhat deficient in that it does not describe the degree of banding or degree of Widmanstätten structure that may be present. Comparison of photomicrographs gave somewhat poorer agreement among different investigators in rating grain size, but such a procedure does permit a partial description of the degree of banding or the presence of Widmanstätten structure. The agreement among investigators using the comparison chart appears to improve as the investigators become more experienced. In other words, some degree of skill is required in using the comparison chart.

J. E. Campbell, R. H. Frazier and **H. O. McIntire,** Metallurgists, Battelle Memorial Institute, Columbus 1, Ohio.

The work was sponsored by the Committee on Ship Steel and done at Battelle Memorial Institute with funds appropriated by the U. S. Bureau of Ships under SR-110 Project on Ship Plate, Contract NObs-53239, Index No. NS-011-078.

Paper was presented at the Ship Structure Session of the 32nd Annual Meeting, AWS, Detroit, Mich., week of Oct. 15, 1951.

METHODS OF DETERMINING GRAIN SIZE

The importance of grain size in steel has been emphasized in many contributions to the literature since Sauveur and Howe[2] measured grain sizes by the planimeter method. When this method was used, the boundary of a group of grains was traversed by a planimeter to obtain the total area, and then the grains within the area were counted. The average area per grain, or the number of grains per square inch, was determined by simple calculations. In work reported by Howe[3] in 1911, an intercept method was used. A line of known length was superimposed on the image of the structure as viewed under the microscope. Grains intersected by the line were counted, the specimen was moved a predetermined distance, and the grains intersected by the line were counted again. This was repeated to obtain a sufficient number of counts to give a satisfactory average. The average intercept length per grain was squared to obtain the average area per grain.

Jeffries and others[4,5] recommended a method of counting grains in a circle drawn on the ground glass of a microscope. The grains within the circle and the boundary grains were counted separately. The number of border grains were multiplied by 0.5, and

the product was added to the number of grains within the circle. Dividing this sum by the area of the circle gave the number of grains per unit area. Jeffries indicated that results obtained by this method were in much better agreement with results obtained by the planimeter method than were results obtained by the intercept method.

Tentative recommendations for determining grain size in metals were prepared by the American Society for Testing Materials in 1917, and the Standard Grain Size Chart for Classification of Steels was adopted in 1933 (ASTM Designation E19–33). These standards were originally intended for measuring austenitic grain size in carburizing steels, but they have also been used for other steels and for nonferrous metals. A relatively simple fracture-grain-size test for tool steels was developed by Shepherd[6] and reported in 1934. Fracture tests for grain size have also been used for steels other than tool steels with satisfactory results.

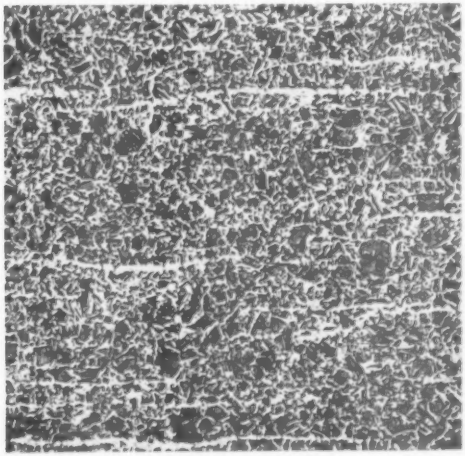
EXPERIMENTAL WORK

Heat Treatments Used in Developing a Series of Structures and Grain Sizes

In earlier work on ship steel, Klier, Wagner and Gensamer normalized ship plate at 1650 and 1750° F. and furnace cooled a specimen from 1850° F. to provide plate having different microstructures.[7] In the present work, a series of specimens having different grain sizes and structures were prepared from Project Steel "A".* Specimens, 6 x 8 1/2 x 3/4 in., were austenitized over a range of temperatures and cooled at different cooling rates. One series of specimens was cooled in air (normalized) after austenitizing at 1400 to 2000° F. Other specimens were cooled in the furnace, in Sil-O-Cel, in an air blast and in water after austenitizing at 1500 to 1900° F. Total heating time for each specimen was 1 1/2 hr. in a furnace preheated to the austenitizing temperature.

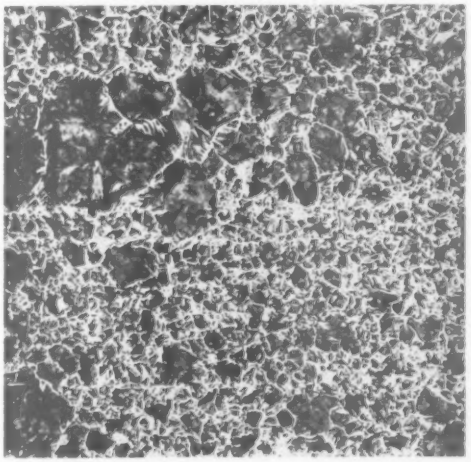
Samples for metallographic examination were prepared from the heat-treated specimens, and photomicrographs at 100 × magnification of the structures in both longitudinal and transverse sections of these specimens were obtained. Nital (2%) was as suitable

* Analysis: carbon, 0.25%; manganese, 0.49%; phosphorus, 0.011%; sulphur, 0.045%; silicon, 0.04%; and nitrogen, 0.004%.



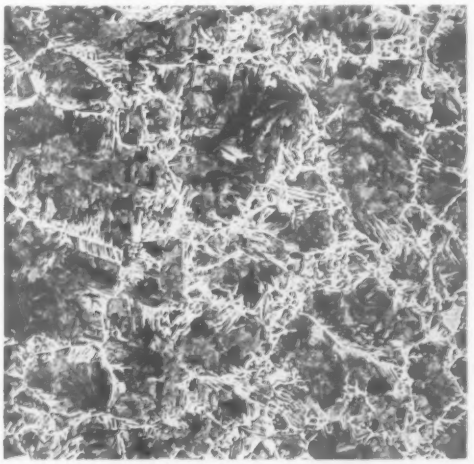
100 × Y3EL 79514

Fig. 1 Structure of specimen that was water quenched after austenitizing at 1500° F. ASTM grain size No. 7. Picral etch



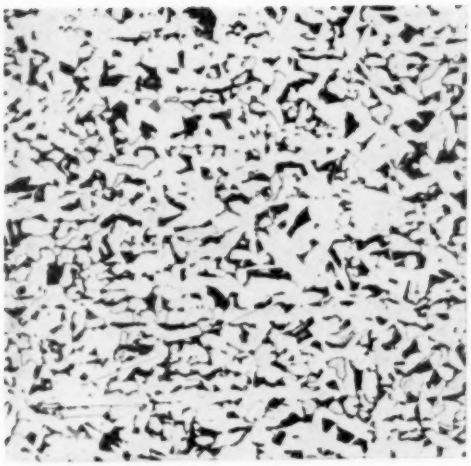
100 × Y4ET 79523

Fig. 2 Structure of specimen that was water quenched after austenitizing at 1600° F. ASTM grain size Nos. 1 to 7. Picral etch



100 × Y5EL 79530

Fig. 3 Structure of specimen that was water quenched after austenitizing at 1700° F. ASTM grain size Nos. 1 to 2. Picral etch



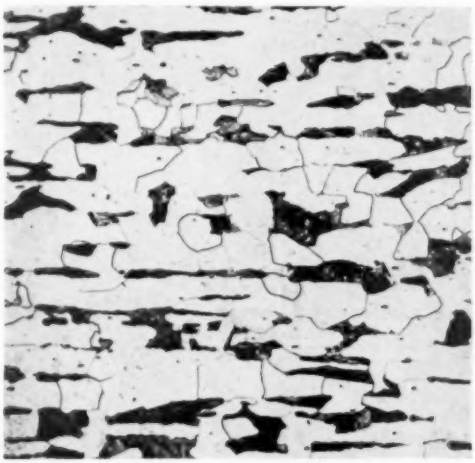
100 × Y1L 78265

Fig. 4 Longitudinal structure of as-rolled plate. Nital etch

for developing the ferrite grain boundaries as any of the various etchants tried. Picral was used as the etchant for the water-quenched specimens.

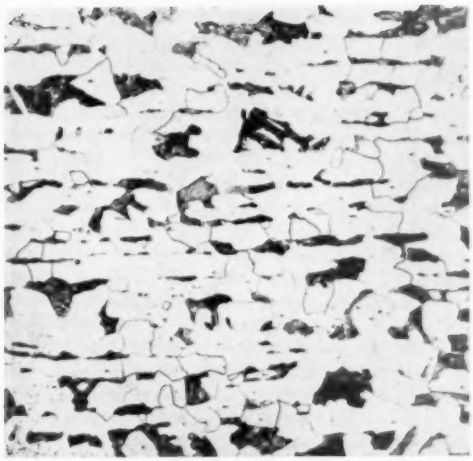
As might be expected, increasing the austenitizing temperature tended to increase the grain size of the austenite. Austenitic grain size developed at the various temperatures was evident in the water-quenched specimens as grains of fine pearlite surrounded by a ferrite network. Figures 1, 2 and 3 show the structures developed after water quenching from 1500, 1600 and 1700° F., respectively. The ASTM grain-size standards are useful in determining the austenitic grain sizes obtained at the various austenitizing temperatures. When comparing these structures with the ASTM Standards, the austenitic grain size obtained at 1500° F. (Fig. 1) is comparable with Grain Size 7. Figure 2 shows the duplex structure resulting from grain coarsening at 1600° F., and contains grains from No. 1 to No. 7 by ASTM Standards. The relatively coarse grain size resulting after austenitizing at 1700° F. is shown in Fig. 3. The longitudinal structure of the as-rolled plate is shown in Fig. 4.

The ferrite grain size which is of particular interest in this work was observed to be dependent on the cooling rate as well as upon austenitizing temperature. Figures 5 through 8 illustrate the different structures



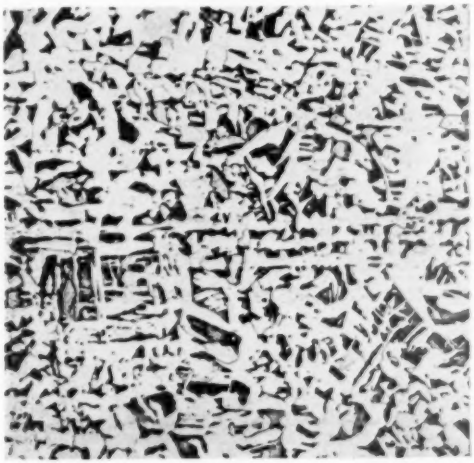
100 × Y5BL 79524

Fig. 5 Longitudinal structure of specimen that was cooled in the furnace after austenitizing at 1700° F. Nital etch



100 × Y5CL 79526

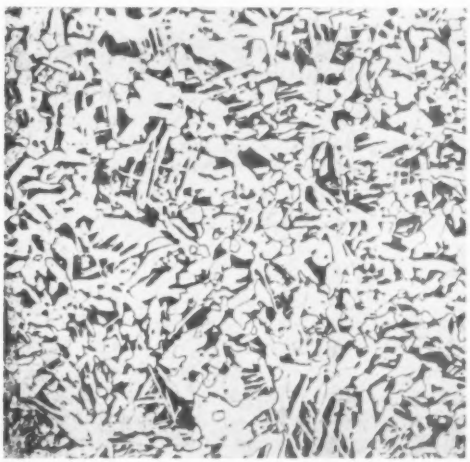
Fig. 6 Longitudinal structure of specimen that was cooled in Sil-O-Cel insulation after austenitizing at 1700° F. Nital etch



100 × Y5L 78267

Fig. 7 Longitudinal structure of specimen that was cooled in air after austenitizing at 1700° F. Nital etch

developed in specimens heated to 1700° F. and cooled at different rates. Not only does the average ferrite grain size become smaller as the cooling rate is increased, but the distribution and shape of the pearlite grains change as well. Slow cooling in a laboratory furnace caused marked banding, as evident in Fig. 5, although the individual ferrite grains tend to be nearly equiaxed. Cooling in Sil-O-Cel insulation, which gives a slightly faster cooling rate, reduced the banding from that obtained by furnace cooling (Fig. 6). As the cooling rate was increased further by cooling the specimens in air, banding was nearly eliminated (Fig. 7). Areas of Widmanstätten structure in which both the pearlite and ferrite grains are elongated in a random pattern occurred when the specimens were cooled in an air blast (Fig. 8)



100 × Y5DL 79528

Fig. 8 Longitudinal structure of specimen that was cooled in an air blast after austenitizing at 1700° F. Nital etch

Counting Methods for Grain-Size Determination

The first method tried in determining the ferrite grain size in ship plate was a modification of the intercept method in which a 2-in. square was marked on a photomicrograph of the structure made at 100 diameters magnification. The top and bottom lines of the square were parallel with the sides of the original plate. Three additional lines were marked off at $^1/_2$-in. intervals within the square, parallel with the top and bottom lines.

The number of individual ferrite grains crossed by the lines at the left and right sides of the square were counted. Then the number of ferrite grains crossed by each of the five horizontal lines were also counted. Additional horizontal lines were counted in an attempt to insure that banding, when present, did not unduly influence the results. The number of grains crossed by the two vertical lines was divided by 4 and the number of grains crossed by the five horizontal lines was divided by 10. The product of these was assumed to be the number of ferrite grains per square inch.

The results obtained by five investigators using the above method on specimens from plate normalized at seven temperatures are given in Table 1. Photomicrographs of sections parallel with the direction of rolling (longitudinal) and at 90 degrees to the direction of rolling (transverse) were used. Investigators A, B, D and E used different prints of the same photomicrographs so that the lines in each photomicrograph were in different locations. Investigator C recounted the same lines counted by A and B. The results illustrate the variations that may be attributed to counting, as compared with variations that may be attributed to the various locations of the lines. Variations in grain size between longitudinal and transverse sections of the same specimens are also indicated. In some instances, the number of ferrite grains per square inch was consistently higher for the longitudinal section than for the transverse section of the same speci-

Table 1—Number of Ferrite Grains per Square Inch at 100 × Determined by the Intercept Method for Normalized Specimens

Normalizing temperature, ° F.	*Section*	*Ferrite grains per square inch determined by investigators*						*Ferrite grains per square inch averaged for longitudinal and transverse sections by investigators*						
		A	*B*	*C (A Lines)*	*C (B Lines)*	*D*	*E*	*A*	*B*	*C (A Lines)*	*C (B Lines)*	*D*	*E*	*Av.*
1400	Long.*	126	138	121	116	77	105	110	135	123	106	75	106	109
1400	Trans.*	95	132	126	95	73	108	..	..	..	..	..	..	..
1500	Long.	95	126	123	116	82	78	122	157	127	136	108	104	126
1500	Trans.	150	188	131	155	134	131	..	..	..	..	..	..	..
1600	Long.	110	132	105	116	75	82	89	100	88	94	68	72	85
1600	Trans.	68	68	72	72	60	62	..	..	..	..	..	..	..
1700	Long.	67	60	63	69	55	47	69	66	66	72	58	46	63
1700	Trans.	72	72	95	76	60	46	..	..	..	..	..	..	..
1800	Long.	52	52	68	56	51	54	60	56	51	64	53	46	55
1800	Trans.	68	60	90	72	55	38	..	..	..	..	..	..	..
1900	Long.	56	46	80	52	57	53	51	46	41	49	46	40	45
1900	Trans.	46	46	68	45	36	26	..	..	..	..	..	..	..
2000	Long.	67	64	89	56	59	36	58	56	39	58	54	43	51
2000	Trans.	47	49	67	60	50	50	..	..	..	..	..	..	..
As-rolled	Long.	79	68	80	76	71	70	65	77	74	63	59	59	66
As-rolled	Trans.	52	86	68	49	47	49	..	..	..	..	..	..	..

* Long. = Longitudinal. Trans. = Transverse.

men. In other instances, the number of ferrite grains was consistently higher for the transverse section of a pair. This is probably indicative of minor variations in grain size from one section of the plate to another, rather than consistent anisotropy within the plates. The averages of the longitudinal and transverse grain-size values obtained by each investigator using the intercept method for each normalized specimen are plotted in Fig. 9 and indicate coarsening in ferrite grain size as the normalizing temperature was increased from 1500 to 1900° F. The spread in results obtained by the five investigators is apparent in the figure.

Grain sizes were also determined for the specimens cooled in the furnace, cooled in Sil-O-Cel and cooled in an air blast using the intercept method described above. Results of intercept counts by two investigators, each using the same prints and the same lines, are given in Table 2, and the averages are plotted in Fig. 10 in relation to austenitizing temperature. These data illustrate that cooling rate, as well as austenitizing temperature, is important in establishing the ferrite grain sizes.

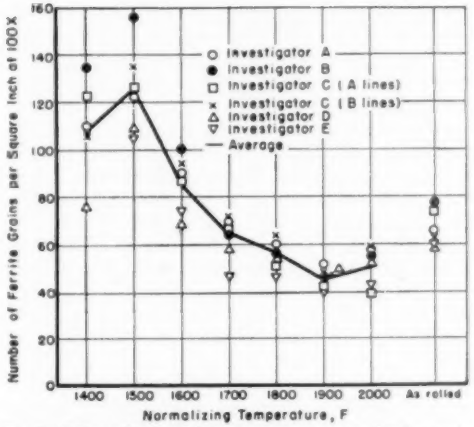


Fig. 9 Effect of normalizing temperature on ferrite grain size. Data were obtained by five investigators using the intercept method

Table 2—Number of Ferrite Grains per Square Inch at 100 × Determined by the Intercept Method

Austenitizing temperature, ° F.	*Cooled in*	*Section*	*Ferrite grains per square inch determined by investigators* B	C	*Ferrite grains per square inch averaged for longitudinal and transverse sections by investigators* B	C	*Av.*
1500	Furnace	Long.*	65	58	67	59	63
1500	Furnace	Trans.*	69	60	..	..	..
1600	Furnace	Long.	45	56	38	44	41
1600	Furnace	Trans.	32	32	..	..	..
1700	Furnace	Long.	21	20	18	21	19½
1700	Furnace	Trans.	15	22	..	..	..
1800	Furnace	Long.	23	27	24	27	25½
1800	Furnace	Trans.	26	27	..	..	..
1900	Furnace	Long.	18	20	14	17	15½
1900	Furnace	Trans.	11	14	..	..	..
1500	Sil-O-Cel	Long.	82	81	80	83	81½
1500	Sil-O-Cel	Trans.	77	85	..	..	..
1600	Sil-O-Cel	Long.	44	52	51	56	53½
1600	Sil-O-Cel	Trans.	58	60	..	..	..
1700	Sil-O-Cel	Long.	30	33	29	33	31
1700	Sil-O-Cel	Trans.	28	33	..	..	..
1800	Sil-O-Cel	Long.	31	36	28	34	31
1800	Sil-O-Cel	Trans.	26	32	..	..	..
1900	Sil-O-Cel	Long.	16	19	15	17	16
1900	Sil-O-Cel	Trans.	14	15	..	..	..
1500	Air blast	Long.	100	100	100	92	96
1500	Air blast	Trans.	100	85	..	..	..
1600	Air blast	Long.	94	90	114	110	112
1600	Air blast	Trans.	135	130	..	..	..
1700	Air blast	Long.	109	85	92	80	86
1700	Air blast	Trans.	75	75	..	..	..
1800	Air blast	Long.	85	85	86	87	86½
1800	Air blast	Trans.	86	90	..	..	..
1900	Air blast	Long.	72	68	72	70	71
1900	Air blast	Trans.	72	72	..	..	..

* Long. = Longitudinal. Trans. = Transverse.

A variation in the intercept method was tried in order to reduce the time required for counting and to eliminate the possible error resulting from banding. A square 2 in. on a side was marked on each photomicrograph, so that the sides of the square were at 45-degree angles with the direction of banding. Two lines were drawn within each square, one being parallel with the upper left and lower right lines and half way between, and the other being parallel with the upper right and lower left lines and midway between them. The number of grains intercepting these lines were then counted as before. Results of the determinations by two investigators using the same diagonal lines in each photomicrograph are given in Table 3. Less time was required for this method, because there were six 2-in. intercept lines instead of seven, but it was still subject to undesirable variations in results by different investigators. These variations were particularly noticeable in the relatively fine-grain specimens.

In analyzing the various intercept methods, it was

Table 3—Number of Ferrite Grains per Square Inch at 100 × Determined by the Intercept Method Using Diagonal Lines

Austenitizing temperature, °F.	Cooled in	Section	Ferrite grains per square inch determined by investigators B	C	Ferrite grains per square inch averaged for longitudinal and transverse sections by investigators B	C	Av.	Average values from Table 2
1500	Furnace	Long.*	81	72	81	68	74½	63
1500	Furnace	Trans.*	81	64	..	..	..	..
1600	Furnace	Long.	53	49	44	42	43	41
1600	Furnace	Trans.	36	36	..	..	..	..
1700	Furnace	Long.	16	16	15	16	15½	19½
1700	Furnace	Trans.	14	16	..	..	..	..
1800	Furnace	Long.	20	20	18	18	18	25½
1800	Furnace	Trans.	16	16	..	..	..	..
1900	Furnace	Long.	25	25	20	20	20	15½
1900	Furnace	Trans.	16	16	..	..	..	..
1500	Sil-O-Cel	Long.	90	81	88	76	82	81½
1500	Sil-O-Cel	Trans.	86	72	..	..	..	..
1600	Sil-O-Cel	Long.	49	49	49	49	49	53½
1600	Sil-O-Cel	Trans.	49	49	..	..	..	..
1700	Sil-O-Cel	Long.	30	36	30	36	33	31
1700	Sil-O-Cel	Trans.	30	36	..	..	..	..
1800	Sil-O-Cel	Long.	36	36	30	33	31½	31
1800	Sil-O-Cel	Trans.	25	30	..	..	..	..
1900	Sil-O-Cel	Long.	20	20	20	20	20	16
1900	Sil-O-Cel	Trans.	20	20	..	..	..	..
1500	Air blast	Long.	132	100	106	86	96	96
1500	Air blast	Trans.	81	72	..	..	..	..
1600	Air blast	Long.	95	81	102	86	94	112
1600	Air blast	Trans.	110	90	..	..	..	..
1700	Air blast	Long.	90	64	90	68	79	86
1700	Air blast	Trans.	90	72	..	..	..	..
1800	Air blast	Long.	90	81	86	72	79	86½
1800	Air blast	Trans.	81	64	..	..	..	..
1900	Air blast	Long.	72	64	76	68	72	71
1900	Air blast	Trans.	81	72	..	..	..	..

* Long. = Longitudinal. Trans. = Transverse.

noted that the lines tended to cross more grains near their edges or corners than near the middle. Thus, the number representing the grains per square inch tended to be too high. The effect may be illustrated by drawing a 2-in. square on graph paper so that the lines of the square are not parallel with the original lines on the paper. Determination of the number of grains per square inch by the intercept method will usually give a result that is too high. Variations in shape and alignment of grains in photomicrographs will determine the amount of error resulting from this effect.

Because of both the experimental and inherent errors associated with the intercept method, ferrite grain sizes were also determined by actual counting of ferrite grains in a given area. A square 2 in. on a side was marked on each photomicrograph with the top and bottom parallel with the sides of the plate. Again, the photomicrographs were all at 100 × magnification. Ferrite grains wholly within each square were counted, and then the ferrite grains intersected by the lines of each square were counted separately. The number of border grains were multiplied by 0.5 and the result was added to the number of grains within the square. This value was divided by 4 to obtain the number of grains of ferrite per square inch at 100 × magnification. Average results of these determinations obtained by two investigators using different prints of the same negatives with the squares only partially covering the same areas are given in Table 4. These results and results obtained using this method for determining ferrite grain size in other specimens of ship plate have shown that better agreement among investigators is obtained by this method than by the intercept method. Because the total count for each photomicrograph is divided by 4, any errors in counting are reduced rather than multiplied. Each grain of ferrite was marked with ink as it was counted,

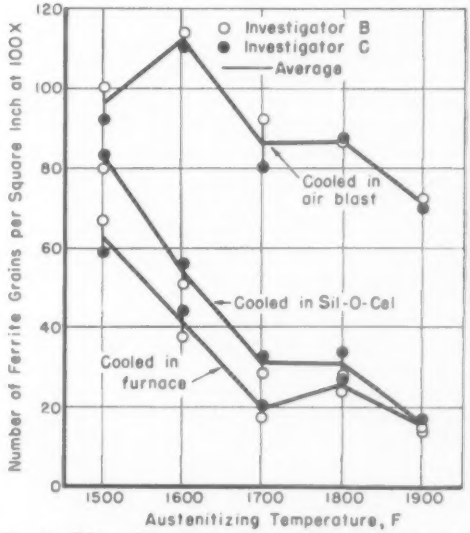


Fig. 10 Effect of austenitizing temperature and method of cooling on ferrite grain size. Data were obtained by two investigators using intercept method

Table 4—Number of Ferrite Grains per Square Inch at 100 × Determined by Counting the Ferrite Grains in a 2-In. Square

Austenitizing temperature, ° F.	*Cooled in*	*Section*	*Ferrite grains per square inch counted by*		*Average of longitudinal and transverse counts by*		*Average for B and C*
			B	*C*	*B*	*C*	
As-rolled	....	Long.*	66, 68	71	58	61	59½
As-rolled	....	Trans.*	48, 48	52	..	..	..
1500	Furnace	Long.	70	73	64½	65	65
1500	Furnace	Trans.	59	58	..	..	..
1600	Furnace	Long.	51	55	38	41½	40
1600	Furnace	Trans.	25	28	..	..	..
1700	Furnace	Long.	14	14	14	13½	14
1700	Furnace	Trans.	14	13	..	..	..
1800	Furnace	Long.	12	14	13½	14½	14
1800	Furnace	Trans.	15	15	..	..	..
1900	Furnace	Long.	18	17	16	15½	16
1900	Furnace	Trans.	14	14	..	..	..
1500	Sil-O-Cel	Long.	91, 88	85	81	84½	83
1500	Sil-O-Cel	Trans.	72, 72	84	..	..	..
1600	Sil-O-Cel	Long.	51	51	48½	50	49
1600	Sil-O-Cel	Trans.	46	49	..	..	..
1700	Sil-O-Cel	Long.	24	30	25½	30	28
1700	Sil-O-Cel	Trans.	27	30	..	..	..
1800	Sil-O-Cel	Long.	22	22	20½	21½	21
1800	Sil-O-Cel	Trans.	19	21	..	..	..
1900	Sil-O-Cel	Long.	12	14	13	14½	14
1900	Sil-O-Cel	Trans.	14	15	..	..	..
1400	Air	Long.	90, 91	103	88	95	91½
1400	Air	Trans.	86, 85	87	..	..	..
1500	Air	Long.	94, 93	98	111	117	114
1500	Air	Trans.	127, 132	136	..	..	..
1600	Air	Long.	88, 89	90	71	77½	74
1600	Air	Trans.	54, 52	65	..	..	..
1700	Air	Long.	54, 53	61	56	63	59½
1700	Air	Trans.	59, 58	65	..	..	..
1800	Air	Long.	50, 51	47	53	53	53
1800	Air	Trans.	57, 56	59	..	..	..
1900	Air	Long.	49, 48	53	43	46½	45
1900	Air	Trans.	37, 37	40	..	..	..
2000	Air	Long.	44, 44	50	42½	47½	45
2000	Air	Trans.	42, 40	45	..	..	..
1500	Air blast	Long.	86	90	82	85	83½
1500	Air blast	Trans.	78, 79	80	..	..	..
1600	Air blast	Long.	84	80	88½	92	90
1600	Air blast	Trans.	93	104	..	..	..
1700	Air blast	Long.	69	72	64	67½	66
1700	Air blast	Trans.	59	63	..	..	..
1800	Air blast	Long.	69	68	62	63½	63
1800	Air blast	Trans.	55	59	..	..	..
1900	Air blast	Long.	67	63	64	64½	64
1900	Air blast	Trans.	61	66	..	..	..

* Long. = Longitudinal. Trans. = Transverse.

and the area within each square was re-examined to make certain that all ferrite grains were counted. The time required for actual counting was 5 to 15 min. for each photomicrograph. The counting time and variation in results will be influenced by the grain size, grain shape and quality of the photomicrograph.

Variations in grain size from one section to another of the same specimen were noted as before in the results obtained by counting the grains. Therefore, the results for both longitudinal and transverse sections of the same specimen were averaged. These results are plotted in Fig. 11. Average results obtained by the counting method were consistently lower than results obtained by the intercept method.

The percentage of pearlite in the photomicrographs was estimated by a point-counting method. Photomicrographs having the 2-in. squares marked on them and the three horizontal lines within each square, as originally used in the intercept method, were used in determining the percentage of pearlite. A trans-

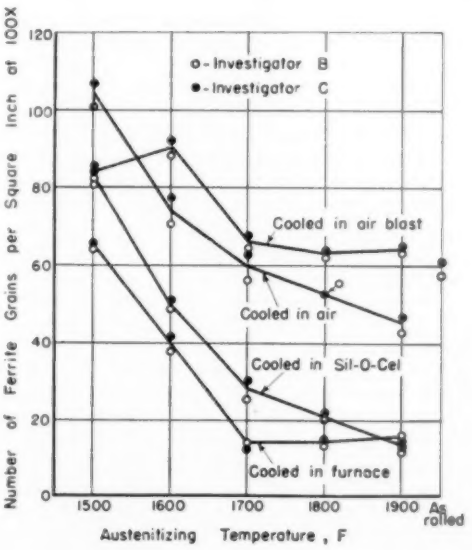


Fig. 11 Effect of austenitizing temperature and method of cooling on ferrite grain size. Data were obtained by two investigators using the counting method

parent grid having 20 lines per inch was placed over each photomicrograph, so that corresponding lines on both the grid and photomicrograph coincided. Points of intersection of the grid lines and the scribed lines on each photomicrograph were examined. If these points coincided with a grain of pearlite, they were counted; if they were on ferrite, they were not counted. A total of 227 points were examined on each photomicrograph. The percentage of pearlite was estimated by dividing the number of points falling on pearlite by 277 and multiplying by 100. This is a simplified version of the point-counting method discussed by Howard and Cohen.[9] Results obtained by two investigators are presented in Table 5

Determination of Ferrite Grain Size by Visual Comparison Using Photomicrographs

Comparison of the grain structure in photomicrographs of production or experimental heats of steel with the grain structure in a series of photomicrographs graded according to grain size is a useful method for determining grain sizes. The ASTM standards, which are based on this method, have been very useful in estimating grain sizes in many steels. However, these standards are not adequate for ship plate, because the gradation in ship plate grain size is relatively small yet appears to have an important effect on its properties. Variations in banding and shape of the ferrite grains may also be taken into account when using the comparison method.

In developing a tentative series of standard photomicrographs, photomicrographs illustrating variations in structure and grain size which might be encountered in ship plate were produced. These photomicrographs are presented in Fig. 12 (*A*), 12 (*B*) and 12 (*C*). The structures at the top of each column have areas of elongated ferrite which are characteristic of Widmanstätten structure. This structure is representative of that obtained by relatively fast cooling or possibly increased alloy content. Five photomicrographs of this structure are shown, covering the range from 40 to 120 grains of ferrite per square inch of image. This section was originally photographed at 100 × magni-

Table 5—Per Cent Pearlite at 100 × Determined by the Grid Method

Austenitizing temperature, ° *F.*	*M* *Cooled in*	*Section*	*Per cent pearlite determined by investigators* *B*	*C*	*Per cent pearlite averaged for longitudinal and transverse specimens by investigators* *B*	*C*
As-rolled	....	Long.*	18	14	18	15½
As-rolled	....	Trans.*	18	17	..	..
1500	Furnace	Long.	18	22	16½	19
1500	Furnace	Trans.	15	16	..	..
1600	Furnace	Long.	10	10	17½	17½
1600	Furnace	Trans.	25	25	..	..
1700	Furnace	Long.	18	19	19	20
1700	Furnace	Trans.	20	21	..	..
1800	Furnace	Long.	17	18	20½	20
1800	Furnace	Trans.	24	22	..	..
1900	Furnace	Long.	22	20	22	22
1900	Furnace	Trans.	22	24	..	..
1500	Sil-O-Cel	Long.	18	14	14½	10
1500	Sil-O-Cel	Trans.	11	6	..	..
1600	Sil-O-Cel	Long.	20	17	16½	14
1600	Sil-O-Cel	Trans.	13	11	..	..
1700	Sil-O-Cel	Long.	19	12	17½	12½
1700	Sil-O-Cel	Trans.	16	13	..	..
1800	Sil-O-Cel	Long.	22	19	22	19
1800	Sil-O-Cel	Trans.	22	19	..	..
1900	Sil-O-Cel	Long.	21	20	19½	17½
1900	Sil-O-Cel	Trans.	18	15	..	..
1400	Air	Long.	19	16†	17	16½
1400	Air	Trans.	15	17	..	..
1500	Air	Long.	19	21	18	18½
1500	Air	Trans.	17	16	..	..
1600	Air	Long.	19	15	18½	17
1600	Air	Trans.	18	19	..	..
1700	Air	Long.	21	17	21	16
1700	Air	Trans.	21	15	..	..
1800	Air	Long.	21	18	21	18
1800	Air	Trans.	21	18	..	..
1900	Air	Long.	21	16	20	16
1900	Air	Trans.	19	16	..	..
2000	Air	Long.	22	16	21	15
2000	Air	Trans.	20	14	..	..
1500	Air blast	Long.	18	14	16½	11
1500	Air blast	Trans.	15	8	..	..
1600	Air blast	Long.	18	15	18	15
1600	Air blast	Trans.	18	15	..	..
1700	Air blast	Long.	20	16	20½	14½
1700	Air blast	Trans.	21	13	..	..
1800	Air blast	Long.	21	18	23	19
1800	Air blast	Trans.	25	22	..	..
1900	Air blast	Long.	18	17	22	17½
1900	Air blast	Trans.	26	18	..	..

* Long. = Longitudinal. Trans. = Transverse.
† Pearlite percentages for normalized specimens determined by Investigator A instead of C.

fication, and the number of grains of ferrite per square inch was determined by the counting method. The five photomicrographs were then obtained at magnifications calculated to show 40, 60, 80, 100 and 120 grains of ferrite per square inch. Variations in grain size in the four types of structure shown were obtained in this way. Ferrite grains in each of the photomicrographs in Figs. 12(*A*), 12(*B*) and 12(*C*) were counted by three investigators, and all but a few of the individual counts were within the values given at the bottoms of the columns.

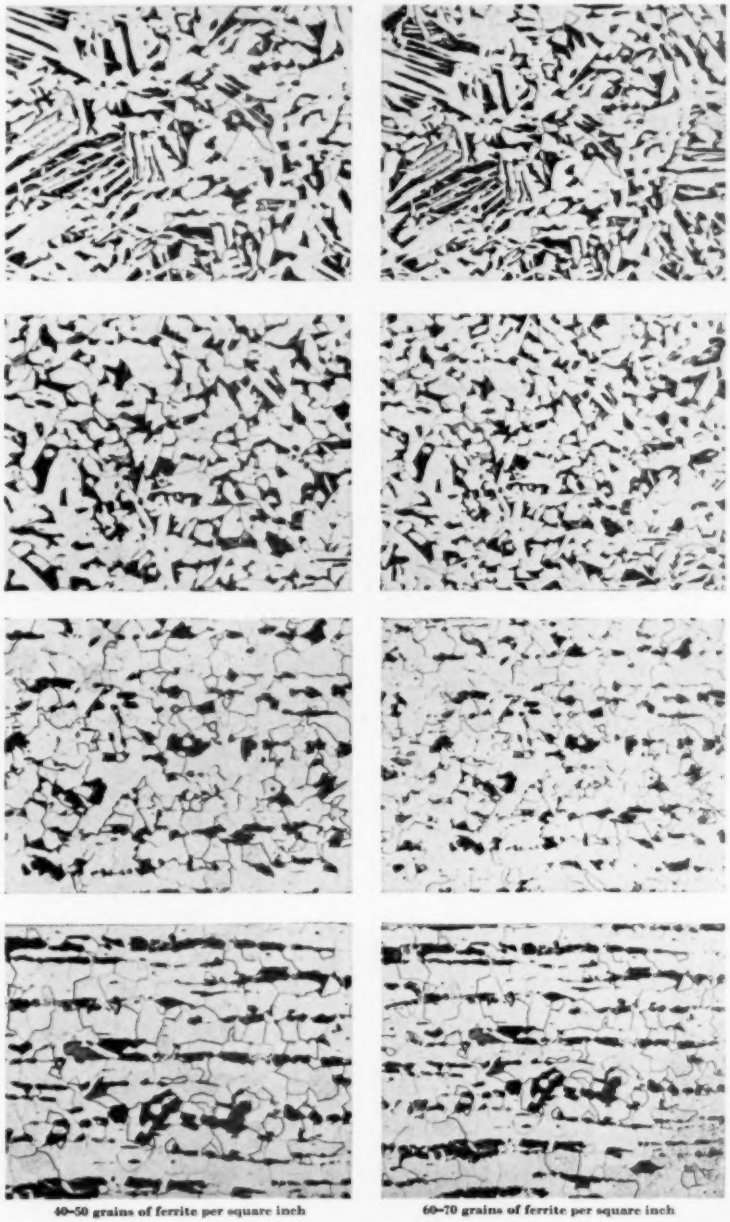

Widmanstätten pattern (1)
Minimum banding (2)
Moderate banding (3)
Heavy banding (4)
40–50 grains of ferrite per square inch
60–70 grains of ferrite per square inch

Fig. 12 (A) Photomicrographs for rating ferrite grain size in ship plate

The second photomicrograph from the top in each column illustrates structures having no Widmanstätten structure and minimum banding. Five photomicrographs of this structure again cover the range from 40 to 120 grains of ferrite per square inch. Structures characteristic of moderate banding are shown in the third photomicrograph in each column and cover the same range of grain sizes as the others. Banding is increased by decreasing the cooling rate or increasing the manganese content. Heavy banding of the pearlite grains is illustrated in the fourth photomicrograph in each column.

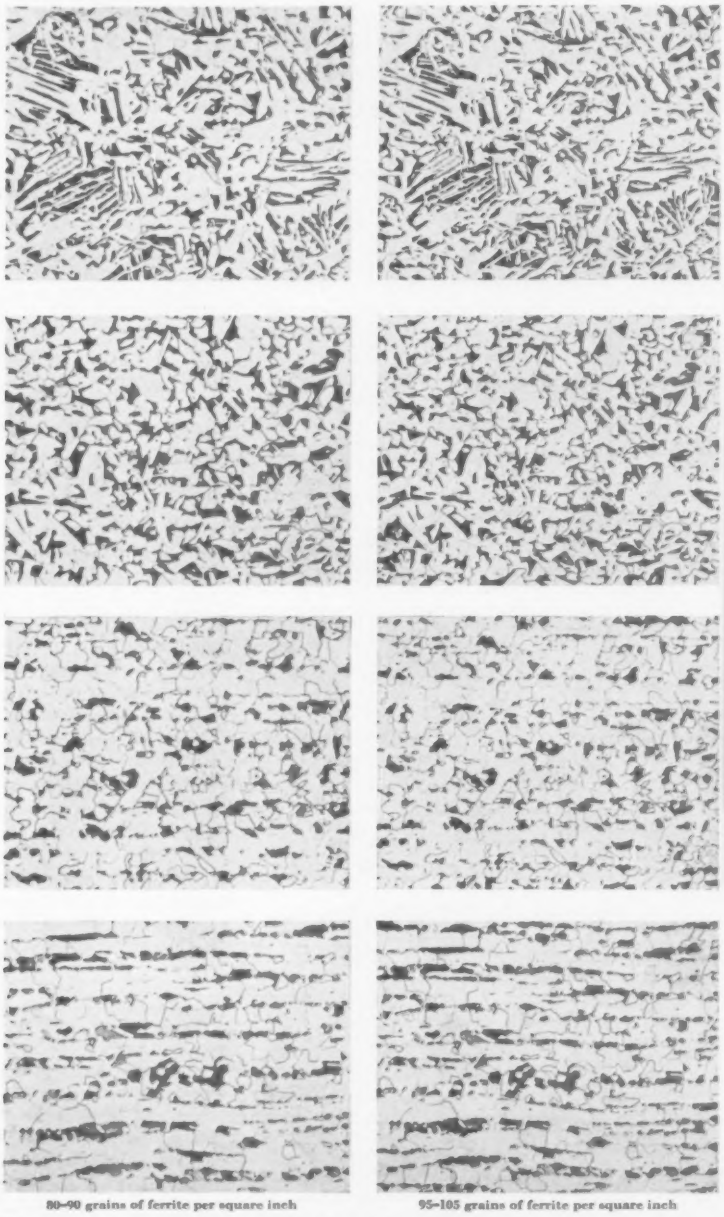

Widmanstätten pattern (1)
Minimum banding (2)
Moderate banding (3)
Heavy banding (4)
80–90 grains of ferrite per square inch
95–105 grains of ferrite per square inch

Fig. 12 (B) Photomicrographs for rating ferrite grain size in ship plate

These variations in structure, illustrating the Widmanstätten pattern and moderate and heavy banding, were obtained by varying the cooling rates as discussed in the section on heat treating. The structure showing minimum banding represents the structure in the as-rolled condition. Photomicrographs of each of the four characteristic structures represent single fields at magnifications selected to give the grain sizes indicated. Photomicrographs of structures of unknown grain size were made at 100 × magnification for comparison with these standards. Etching and photographing techniques which give the grain boundaries and pearlite about the same amount of contrast as shown in the series of photomicrographs are desirable. When comparing ferrite grain sizes, attention should be concentrated on the ferrite grains themselves, and not on the relative spacing between pearlite grains. Comparison should be made between the maximum and minimum sizes of ferrite grains, as well as the average grain size, in the unknown and standard photomicrographs. In addition to rating the grain size of unknown specimens, the structures shown in the chart will aid in rating the structures of the unknown specimens as to degree of banding or the presence of Widmanstätten structure.

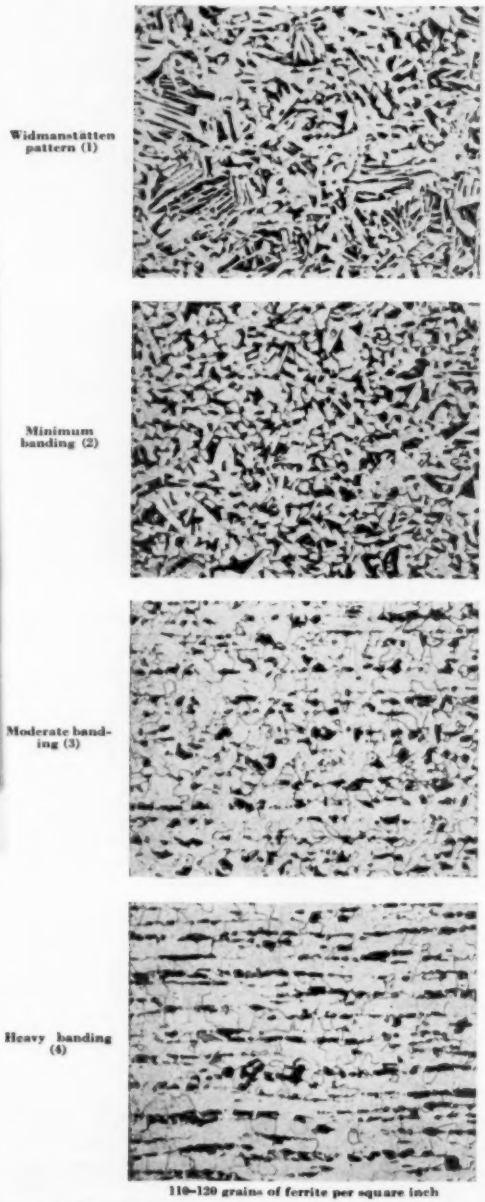


Fig. 12 (C) Photomicrographs for rating ferrite grain size in ship plate

Twelve photomicrographs of commercial and experimental ship plate of various structures and grain sizes were compared with this series of photomicrographs by seven investigators. The ratings obtained by each investigator are presented in Table 6. Ferrite grain sizes in these photomicrographs were also determined by the counting method and are given in the second column of the table. The range in grain-size ratings for each photomicrograph is given in the last two columns in the table. Of the ten photomicrographs having less than 100 grains of ferrite per square inch, 53 of the 70 ratings, or 76% were within 10 grains per square inch of the value determined by the counting method. Photomicrographs having more than 100 grains of ferrite per square inch apparently are more difficult to rate by this method. However, as one gains experience, agreement with the results obtained by the counting method becomes better.

The percentage of pearlite in the photomicrographs in Fig. 12 is within the range of 14 to 23%. This is normally the amount of pearlite that may be expected in ship plate, and utility of this chart is limited to specimens having pearlite in this range. Additional sets of photomicrographs, having higher percentages of pearlite or different structures or grain sizes, may be prepared by the same method if desired. When using Fig. 12 for determining grain size, comparison of microstructures in both longitudinal and transverse sections of the unknown material is recommended.

Determination of Ferrite Grain Size by Fracture Test

Fracture grain sizes of the heat-treated specimens were determined on samples about 2.2 in. long, 0.6 in. thick and $^3/_4$ in. wide (thickness of the plate). These samples were notched with a saw so that when they were broken, the fracture would be $^3/_4$ x 0.45 in. Both longitudinal and transverse samples were obtained from the normalized plate, and only longitudinal samples were obtained from specimens having the

Table 6—Results of Rating Grain Size Using Photomicrographs in Fig. 12

Structure*	Grains of ferrite per square inch by count	Grain-size ratings by investigators†							Minimum	Maximum
		B	F	G	H	I	J	K		
2–3	119	2/95–105	2–3/95–105	2–3/110–120	3/95–105	2/90–95	3/95–105	2–3/95–105	90–95	110–120
2	87	2/80–90	2/80–90	2–3/80–90	3/80–90	2/80–90	2–3/95–105	2/70–80	70–80	95–105
2	73	2/50–60	2/50–60	2/60–70	3/40–50	2/60–70	2–3/80–90	2/60–70	40–50	80–90
3–4	132	3/110–120	4/110–120	4/120+	3–4/95–105	3–4/95–105	3–4/110–120	3–4/110–120	95–105	120+
3	99	3/95–105	3/95–105	3/110–120	3/95–105	3/95–105	3/110–120	3/95–105	95–105	110–120
3	76	3/80–90	2–3/60–70	3/95–105	3/80–90	3/80–90	3/95–105	3/80–90	60–70	95–105
2	62	2/50–60	2/60–70	2/40–50	2/40–50	2/40–50	2/60–70	2/40–50	40–50	60–70
2	79	2/60–70	2/60–70	2/70–80	2/60–70	2/60–70	2/80–90	2/60–70	60–70	80–90
1–2	48	1–2/60–70	1–2/60–70	1–2/50–60	1–2/60–70	1–2/60–70	1–2/40–50	1–2/50–60	40–50	60–70
1–2	55	1–2/40–50	1–2/40–50	1/40–50	1–2/40–50	2/40–50	2/60–70	1–2/40–50	40–50	60–70
1	69, 72	1/60–70	1–2/80–90	1–2/70–80	1/80–90	1/80–90	1–2/95–105	1/50–60	50–60	95–105
4	70, 73	4/70–80	4/80–90	4/60–70	4/60–70	4/60–70	4/80–90	4/70–80	60–70	80–90

* 1 = Widmanstätten structure, 2 = minimum banding, 3 = moderate banding, 4 = heavy banding.
† Structure is rated first as 1, 2, 3 or 4, and number of ferrite grains is rated second according to classification in Fig. 12.

other heat treatments. These bars were cooled in liquid nitrogen (boiling point −320° F.) for at least 5 min. after boiling stopped. They were removed from the liquid nitrogen one at a time, quickly placed across a 2-in. channel iron and broken with a blow of a ball-peen hammer. The fractured pieces were placed in alcohol to prevent condensation and rusting. After they reached room temperature, they were placed in a desiccator.

The fractures were of the granular type and were light gray in color, interspersed with bright facets which probably were cleavage planes. The fractures were examined with the unaided eye. The sequence of the changes in the fracture appearance after different austenitizing temperatures were more apparent for the longitudinal specimens than for the transverse specimens. For this reason, transverse samples were not prepared from the specimens having heat treatments other than normalizing.

The fractures were rated using standard Shepherd fracture specimens and estimating the fracture grain size to half of a grain-size number. Standard and unknown fracture specimens were compared under low-power binoculars. Results obtained by five investigators in rating fracture grain sizes are given in Table 7. In Fig.13, averages of these results were plotted in relation to the average of actual grain-size counts. The effect of variation in structure, as well as grain size, on the apparent fracture grain size is illustrated in this figure. If a fracture-grain-size rating of 3 is obtained the number of ferrite grains per square inch may be in the range from 65 to 92, depending on the characteristics of the structure. Effects of variation in the structure on the fracture-grain-size rating have also been noted by Grossman[10] in hardened and tempered steels.

Shepherd grain-size numbers and the ASTM grain-size numbers correspond when considering grain size at

Table 7—Fracture Grain-Size Ratings Using Shepherd Grain-Size Standards

Austenitizing temperature, °F.	Cooled in	Fracture grain-size ratings by investigators						Av. No. of ferrite grains per square inch†
		B	D	F	G	H	Av.*	
As-rolled	....	3	3	3½	3	3	3	59½
1500	Furnace	3	3	3	3½	3	3	65
1600	Furnace	2½	2	2	2½	2	2¼	40
1700	Furnace	1½	2	1½	2	1	1½	14
1800	Furnace	..	..	..	..	..	..	14
1900	Furnace	1	2	2	2	1	1½	16
1500	Sil-O-Cel	3	3	3	3	3	3	83
1600	Sil-O-Cel	2½	2	2½	2½	2½	2½	49
1700	Sil-O-Cel	1½	2	2	2	2	2	28
1800	Sil-O-Cel	1	1½	2	2	2	1¾	21
1900	Sil-O-Cel	1½	2	1½	2	2	1¾	14
1400	Air	3	2	3	3	3½	3	91½
1500	Air	3½	3	3½	3½	4	3½	114
1600	Air	2½	2	3	2½	3	2½	74
1700	Air	2	2	2½	2½	3	2½	59½
1800	Air	2	2	2	2	2	2	53
1900	Air	2	2	2	2	2	2	45
2000	Air	1½	2	1½	2	1½	1¾	45
1500	Air blast	3	3	3	3	4	3¼	83½
1600	Air blast	3	3½	3	3	3	3	90
1700	Air blast	2½	2	2	2½	3	2½	66
1800	Air blast	1½	2	2	2½	2	2	63
1900	Air blast	2	2	2½	2	2½	2¼	64

* Average to nearest ¼.
† Determined by counting method.

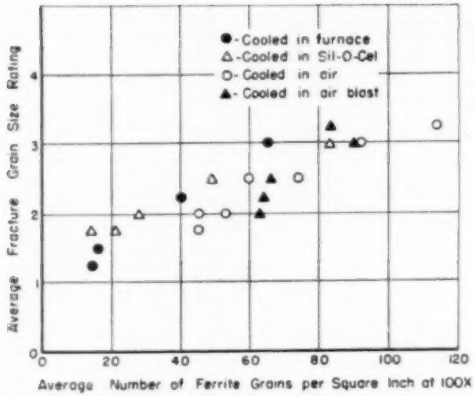


Fig. 13 Relationship between average fracture grain size rating and ferrite grain size determined by the counting method

austenitizing temperatures. Fractures in the ship plate specimens were within fracture Grain Size Nos. 1 to 4, as shown in Table 7. Therefore, the fracture grain size is more nearly representative of the austenitic grain size than the ferrite grain size. Even if the fracture standards were made of ship plate steel instead of tool steel, variations in structure from that having a Widmanstätten appearance to that having a banded condition would result in discrepancies in evaluating ferrite grain size by the fracture method.

CONCLUSIONS

In conclusion, the results of this work may be summarized as follows:

1. The average ferrite grain size was found to be dependent on the cooling rate as well as on the austenitizing temperature.
2. When the ferrite grain size was rated by intercept methods, grain counts, comparison with special photomicrographs and fracture methods, the counting method was the most reliable.
3. The comparison method is less accurate than the counting method, but it is quicker and the type of structure may also be rated.
4. The fracture method was not suitable for determining ferrite grain size in ship plate.

ACKNOWLEDGMENT

The authors wish to express their appreciation to the Ship Structure Committee, who sponsored this work, and to the Committee on Ship Steel and the Subcommittee on Grain Size, under whose guidance the project was conducted, for granting permission to publish these results. The authors are also grateful for the help received from the Battelle Memorial Institute staff, especially Dr. C. H. Lorig, G. K. Manning and F. W. Boulger, in conducting the project and in preparing this paper.

References

1. "Standard Classification of Austenite Grain Size in Steels," ASTM Designation: E19–46; ASTM Standards, 1949, Part I, pp. 1271–1273.
2. Sauveur, A., and Howe, H. M., "The Relation Between Temperature and the Grain of Steel," *Eng. Mining J.*, p. 537 (Dec. 7, 1895).
3. Howe, H. M., "Life History of Network and Ferrite Grains in Carbon Steel," *Proc. ASTM*, **XI**, 262–386 (1911).
4. Jeffries, Z., Kline, A. H., and Zimmer, E. M., "The Determination of Grain Size in Metals," *Trans. AIME*, **54**, 594–607 (1917).
5. Jeffries, Z., "Grain Size Measurement in Metals," *Chem. Met. Eng.*, **18**, 185 (1918).
6. Shepherd, B. F., "The P-F Characteristics of Steel," *Trans.* ASM, **22**, 979–1016 (1934).
7. Klier, E. P., Wagner, F. C., and Gensamer, M., "The Correlation of Laboratory Tests with Full-Scale Ship Plate Fracture Tests," THE WELDING JOURNAL, **27**, (2), Research Suppl. 71-s–95-s (1948).
8. Kahn, N. H., and Imbembo, E. A., "Notch Sensitivity of Steel Evaluated by Tear Test," *Ibid.*, **28** (4), Research Suppl., pp. 153-s to 166-s, 177-s (1949).
9. Howard, R. T., and Cohen, M., "Quantitative Metallography by Point Counting and Lineal Analysis," *Metals Tech.*, **14**, T. P. 2215 (August 1947).
10. Grossman, M. A., "Toughness and Fracture of Hardened Steels," *Trans. AIME*, Iron and Steel Division, **167**, 39–79 (1946).

Discussion by S. Epstein and W. P. Gerhart

The authors are to be commended for having made such a good start on this task. The microstructure of ship plate steel, and particularly the grain size, obviously has a most important influence on its notch resistance. Yet the structures encountered in ship plate are quite varied, especially the Widmanstätten type, so that it is often difficult to decide which of two steels has the coarser structure and which the finer even though the two steels differ markedly in notch resistance. The methods described in this paper for actually measuring the ferrite grain size should therefore be very helpful in such cases and, in general, in reaching a clearer and more definite understanding of the influence of the structure of these steels on the notch resistance.

Once the ferrite grain size is established it may become possible to separate the effect of the ferrite grain size on the notch resistance from the effects of the other variables—namely the content of carbon and other alloying elements; the degree and type of deoxidation; the amount of reduction in hot rolling; the shape, size and arrangement of the microconstituents; the degree of hardening of the pearlitic or bainitic constituent; the occurrence of precipitation of carbides or nitrides in the grain boundaries or on the slip planes of the ferrite; etc. All this is mentioned not to unduly complicate matters but to make clear that though grain size measurements should be very helpful, they do not already indicate how the notch resistance of as-rolled commercial ship plate may be readily improved. Much more study and development will have to be done before that can be accomplished.

In line with these comments it would have been of

S. Epstein and **W. P. Gerhart** are connected with the Bethlehem Steel Co., Bethlehem, Pa.

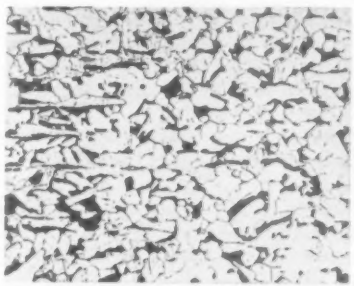
interest to show the actual micrographs of the 12 commercial ship plates whose grain sizes are rated in Table 6 of the paper. As it is, only the grain counts are given in Table 6 and the results of the comparisons between the structures of the steels and the "standardized" micrographs of Fig. 12. The wide range of grain sizes of the "standardized" micrographs of this figure (Figs. 12 (*A*) to 12 (*C*)) was not obtained from samples of actual commercial steels at the single magnification of × 100, but by changing the magnifications of only 1 set of 4 samples, all from one steel given four different heat treatments. While the "standardized" micrographs of Fig. 12 may serve well the immediate purpose for which they were intended of showing a range of grain sizes in structures resembling those of commercial ship plate, they do not seem to give sufficient information for relating the structure and grain size of the 12 commercial steels with the respective notch resistance of these 12 steels. It would also, of course, have been of interest to give the transition temperature of those 12 steels and thus to show how these transition temperatures correlate with the grain size ratings.

Despite these lacks, however, and as the work continues the authors will no doubt fill them, it is really surprising how much that is *definite* and hence more useful may already be deduced from the results of the grain size measurements shown in the paper. As an example we may take Fig. 11, showing the grain size measurements of a semi-killed American Bureau of Shipping Class A plate steel in the as-rolled condition and as subsequently heated to different austenitizing temperatures from 1500 to 1900° F. and cooled at faster and slower rates. It is well known that reheating as-rolled steel to higher austenitizing temperatures produces grain coarsening, that slower cooling after such heating gives a somewhat larger grain, and that such slower cooling materially lowers the notch resistance. But graphically plotting the results of actual grain size measurements as in Fig. 11, gives this knowledge new clarity and emphasis and greatly enhances it.

The point most strongly emphasized by Fig. 11 is the great effect of slow cooling in coarsening the grain. Slow cooling in Sil-O-Cel from *1700° F.* gave much coarser grain than cooling in air from *1900° F.* And furthermore, even high reheating temperatures, up to 1900° F., did *not* cause pronounced grain coarsening when a fairly fast cooling rate was used as air cooling or cooling in an air blast. The importance of this is the more apparent when it is realized that the reheating temperature under these conditions probably gives a grain which corresponds fairly well with the grain which is produced when the finishing temperature in rolling is the same as this reheating temperature. Thus, since the as-rolled grain size in Fig. 11 corresponds to that obtained by reheating to 1700° F. and air cooling, it seems justified to assume that the finishing temperature in rolling was not far from 1700° F.

The data in Fig. 11, therefore, emphasize that more benefit may perhaps be derived from an effective method of faster cooling (at a rate similar to the air cooling of the specimens of this paper) than by controlling the finishing temperature in rolling.

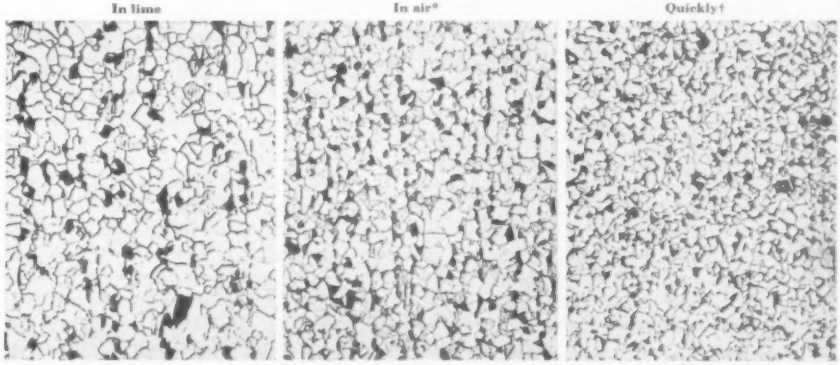
The data and micrographs on the accompanying figure illustrate how greatly slower cooling rates may



Rockwell B hardness: 63
Ferrite grains per square inch: 70
Transition temperature, ° F.: 0

Heated to 1700° F. and cooled

In lime	In air*	Quickly†



* A ¾-in. plate, 12 in. long and 6 in. wide, was cooled in air from 1700° F.
† The small unnotched Charpy specimens were cooled in air from 1700° F.

	In lime	In air*	Quickly†
Rockwell B hardness:	58	67	69
Ferrite grains per square inch:	70	100	130
Transition temperature, ° F.:	23	—19	—60

decrease the notch resistance. The steel was a $^3/_4$-in. thick Class A ABS semi-killed plate steel of 0.15% C, 0.47% Mn, 0.018% P, 0.024% S, 0.06% Si and 0.008% Al content. The grain size counts were obtained by comparing the micrographs with the "standardized" micrographs of Fig. 12 of the paper. The transition temperatures were determined with Charpy keyhole specimens at 20 ft.-lb. energy absorbed. The ferrite grain size in the as-rolled condition was approximately the same as in the specimen heated to 1700° F. and slowly cooled in lime, but nevertheless the slow cooling in lime resulted in a considerably higher transition temperature. The specimen cooled in air after heating to 1700° F. had a somewhat finer grain size than the as-rolled sample and an appreciably lower transition temperature. The specimen "quickly" cooled from 1700° F. had a still finer grain and a much lower transition temperature.

In this case the heating to 1700° F. (for $^1/_2$ hr. instead of the $1^1/_2$ hr. heating time used in the paper) followed by fast cooling did not produce a Widmanstätten type of structure similar to that which was present in the steel in the as-rolled condition. It is a question, therefore, whether with a Widmanstätten structure commonly present in as-rolled plate, the benefit from faster cooling would be as pronounced as is indicated in the accompanying figure. Much more work on the various types of plate steels and plate steel structures will, therefore, have to be done to establish the benefits that may be derived from faster cooling.

It remains to mention the great practical difficulty of accomplishing effective faster cooling in commercial plate mills. After the finishing pass the plates must be straightened and every effort is made to do this while the plate is still hot. Thus there is not much room to accomplish faster cooling, and too fast cooling adds to the difficulties in straightening. Nevertheless the emphasis this paper casts on this phase of plate steel manufacture may yet lead to beneficial results.

Discussion by Stuart V. Arnold

A convenient and accurate method for measuring ferrite grain size has long been sought by the metal industry. Research in this direction has, I believe, been impeded by the unfortunate tedium associated with accumulating and analyzing data. Certainly those who contribute their efforts to advance this phase of technology are to be commended. The problem of properly defining structures encountered in ship plate is perhaps more complicated than usual owing to the variety of micro constituent forms and presence of local inhomogeneities.

To one perhaps not fully acquainted with the purpose of the subject paper, it is regrettable that objectives, interpretations and conclusions are not more sharply defined. I, for one, should like to know what use is intended for the findings presented. If it is proposed, for instance, to adopt the comparison series of photomicrographs as a standard for measuring grain sizes of ship plate steel, then I should wish to raise certain objections. Whereas the tentative methods for estimating the average ferrite grain size of low-carbon steels, as described in ASTM Publication E89–50T issued in 1950 may not entirely suffice in this particular case, it is held advisable to coordinate efforts toward modifying this standard to accommodate the situation rather than to develop a separate standard for this committee.

If next we consider the bases on which the photomicrographs for rating ferrite grain size were selected in the subject report, there is further ground for discussion:

Four structures were arbitrarily selected as representative. One disclosing a typical Widmanstätten pattern is distinct from the others. The remainder exhibit, in addition to a certain grain size, various degrees of banding. These degrees were arbitrarily selected. It is suggested that segregation of pearlite areas into bands could be evaluated on a statistical basis by a variation of lineal analysis methods discussed by Howard and Cohen in their paper "Quantitative Metallography by Point Counting and Lineal Analysis." An attempt in this direction should be made in order to judge degree of banding on a truly comparative basis.

The use of the four photomicrographs selected as representative to rate a variety of grain sizes through the mechanism of providing as references prints of these structures in several magnifications is unsound. The size and character of the ferrite and pearlite constituents is determined by the cooling rate which maintains during their formation. It does not follow that the size, shape and disposition of pearlite will remain in the same relation to like features of the ferrite at a variety of cooling rates. Similarly, if one associates banding of pearlite with micro-segregation, it is not reasonable to expect this factor to vary with cooling rate, at least not in any fashion directly related to the ferrite grain size.

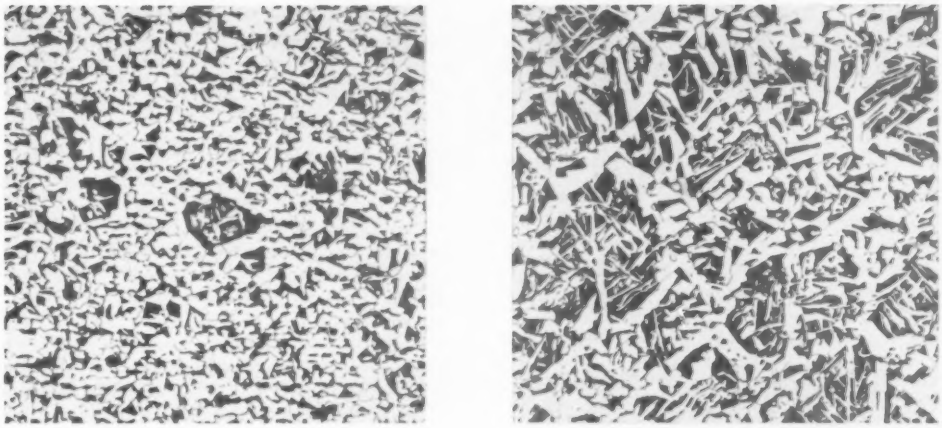
Discussion by K. J. Stodden and R. W. Vanderbeck

We are very glad to see that the authors are working on the problem of determining ferrite grain size more precisely. Development of a satisfactory rating method should permit evaluation of the influence of grain size in the investigation that Battelle has undertaken to determine the effects of deoxidation practice and of composition on the notched-bar properties of ship-plate steel.

The problem of grain-size determination is of general

Stuart V. Arnold is with the Metallurgical Processes Br. of the Watertown Arsenal Laboratory.

K. J. Stodden and **R. W. Vanderbeck** are connected with the U. S. Steel Corp., Pittsburgh, Pa.



Grain Size 7 Grain Size 6

Fig. 1 Microstructure of a 0.20% carbon structural steel heat treated to different grain sizes. $\times 100$

interest because it is now fairly well established that the notch toughness of a steel, as measured by transition temperature, depends on the size of the ferrite grain. For example, Hodge, *et al*,* report that the keyhole Charpy ductility-transition temperature of a low-carbon steel and of a nickel steel is lowered 30° F. by a decrease in ferrite grain size equal to a unit increase in ASTM grain-size number, the change in ferrite grain size being obtained by heat treatment. We have obtained similar results on various carbon plate steels, but, frequently, considerable scatter is obtained. To determine how precise the relationship between ferrite grain size and transition temperature is and to increase the usefulness of such a relationship in interpreting the findings of research, it seems necessary to rate ferrite grain size much more accurately than can be conveniently done at present.

We, also, have been working on this problem and the following comments are offered in the hope that they will be of some assistance in the Battelle study.

Although the grain size range ASTM 7 to 8, discussed by the authors, is found in light-gage plate, a coarser grain size is usually obtained in plates of greater thickness. Therefore, the value of this study might be increased if the scope of the work were broadened to include these coarser grain sizes. As a matter of interest, we would like to know what method of evaluation the investigators would use if mixed grain sizes were involved.

Messrs. Campbell, Frazier and McIntire obtained different grain sizes in the comparison chart for the Widmanstätten-type microstructure simply by changing the magnification of a single Widmanstätten pattern. Differences in Widmanstätten patterns, however, generally include changes in the amount and distribution of Widmanstätten ferrite, as well as changes in the amount and distribution of Widmanstätten ferrite as well as changes in the size and shape of the grains. This is illustrated in Fig. 1 where the Widmanstätten ferrite, although present in both microstructures, appears to a much lesser extent in the finer grained material. The distribution of the Widmanstätten ferrite within the Widmanstätten areas also varies.

Since the Widmanstätten patterns that are generally observed in plate steels vary considerably, inclusion of a number of Widmanstätten-type microstructures would seem to be advisable for the preparation of standards that would cover the full range of microstructures obtained in practice.

It seems probable that the use of statistics in analyzing the data would be a much more revealing way of evaluating the uncertainties in grain-size measurements. Statistics would indicate, within specified certainty limits, whether real differences existed in the amount of scatter or in the average grain count among the different methods, the different observers, the different samples from the same material and the different materials. The appropriate statistical design should preferably be specified beforehand so that the influence of each of these variables may be properly evaluated.

It is obvious that two steels can contain the same number of ferrite grains per unit area yet not have the same actual grain size because of different amounts of pearlite. The authors in this paper have apparently not attempted to calculate the actual size of the average ferrite grain, but they obviously recognize that the amount of pearlite does affect the rating, for they have reported the range of the percentages of pearlite present. The fact that the pearlite percentage covered a range (15 to 25%) will in itself introduce variability in the ratings by the authors' methods. Furthermore, an evaluation of the actual grain size will not be

* Hodge, J. M., Manning, R. D., and Reichhold, H. M., "The Effect of Ferrite Grain Size on Notch Toughness," *Trans. AIME*, **185**, 233–240 March 1949).

obtained unless consideration is given to the amount of pearlite present. To compensate for the amount of pearlite present, would it not be better to apply a correction to each grain-size rating?

Authors' Closure

The authors wish to thank those who have had a part in preparing discussions on this paper. The various structures, grain sizes, grain-size distribution and grain shapes that occur in ship plate make the problem of grain-size determination particularly difficult.

This work on ferrite-grain-size determination has been done at Battelle in conjunction with another phase of the project dealing with the effect of deoxidation practice and alloying additions on the transition temperatures of ship plate. Variations in grain size, as well as deoxidation procedures and composition, are to be correlated with transition temperatures. Effects of variations in ferrite grain size and structure on the transition temperature are being determined for a later report. This report was limited to methods which we have tried to use in measuring the ferrite grain size in ship plate, and unfortunately this work was limited to $^3/_4$-in. plate.

We have used the counting method for the determination of ferrite grain size whether the grains were fairly uniform in size or of mixed grain size. However, the authors wish to emphasize again that if the amount of pearlite varies from the usual amount, i.e., about 20%, this variation must also be taken into account. The Widmanstätten patterns are often associated with plate of relatively high carbon content and consequently higher percentages of pearlite. Expanding the grain-size chart (Fig. 12) to include coarser grain sizes, additional types of Widmanstätten patterns and various ranges of pearlite percentages would, no doubt, make this type of chart more useful. The grain-size chart was prepared in order to determine the degree of success that would be achieved with such a method.

Data obtained by the intercept method were originally developed statistically, but since we believe this method of grain-size determination to be misleading, we have not discussed the statistical evaluation. Data obtained by the counting method on any one or several photomicrographs have not been complete enough for statistical evaluation. However, in comparing the results obtained by the intercept and counting methods, the variation in results obtained among investigators was considerably less when using the counting method. Better agreement was obtained when the ferrite grains were relatively equiaxed than when they occurred in a Widmanstätten pattern.

The methods discussed in "Tentative Methods for Estimating the Average Ferrite Grain Size of Low-Carbon Steels," ASTM Designation E89–50T,* include the comparison method, the Jeffries planimetric method and Heyn's intercept method. The comparison chart is based on the ASTM grain-size numbers and shows standard photomicrographs of equiaxed ferrite grains with no pearlite present. The authors consider the type of chart shown in Fig. 12 to be more suitable for ship plate. Jeffries' method of grain counting and use of the Heyn intercept method have already been discussed.

The authors are particularly indebted to Mr. Epstein and Mr. Gerhart for showing how the control of grain size in ship steel may be important in obtaining plate of lower transition temperature.

* 1950 Supplement to *Book of ASTM Standards Including Tentatives*. Part I, Ferrous Metals, p. 304.

Welded Reinforcement of Openings

(*Continued from page 77-s*)

nificance. All specimens with square cornered openings both with and without a corner radius involved four points of principal strain concentration. The circular openings involved only two such points. If instead of circular openings, parallel sided openings with semicircular ends had been employed where the parallel sides were in the direction of loading, four points of principal strain concentration would again be present. This may mean that the two principal strain concentration locations of the circular openings involve superposition effects. If this is true, the applicability of the circular opening specimens to the present series is questionable.

While the above comments are pretty much on the critical side, I want to say at this point that with the program as it was laid before them, the investigators have done a tremendous job and uncovered considerable valuable data. It is gratifying to find experimental indications that our current design practices are on the right path. It appears unnecessary to further comment on the value of the current undertaking.

Fracturing and Fracture Dynamics

Fracturing begins at flaws and is accompanied by considerable plastic deformation. The progressive extension of a fracture requires little driving energy which, however, may be assessed

by G. R. Irwin and J. A. Kies

UNDER the terminology used in this paper, fracturing is the separating process, usually progressive, through a material which results in a fracture. One may take a still photograph of a fracture or a motion picture of fracturing. Fracture dynamics is the knowledge of fracturing which can be organized in an understandable fashion in terms of known principles of mechanics.

From a descriptive viewpoint, it is possible to study in detail the component parts of fracturing at any stage which is large enough to permit observations. When one attempts to extend the descriptive study to include the microscopic origin of fracturing, certain general characteristics may be found but the details are obscured. Regardless of obscurity of details it is known that initial separations occur in a scattered independent fashion in regions of high tensile stress; that they join up to form what is commonly called a crack, and that continued reinitiations are a dominating feature of progressive fracturing. These reinitiation features are illustrated in considerable detail in reference 1.

One thinks of a brittle fracture in, say, a solid block of cellulose acetate as a razor thin separating crack spreading rapidly. However, the large local deformations at the origins of fracture elements suggest that at a very early stage of development, the separation is not of the thin disk type but is a nearly equiaxed opening. An example of this is provided in Fig. 1. Part (*a*) shows a parabola-like fracture element in cellulose acetate originating from or starting with the growth of a small cavity. The intention of this figure is to model a feature existing usually on a finer scale in metals. However, this feature may be gross and easily observed as in copper, lead-tin solders and some steels. The same kind of orgin in a broken tensile bar of steel is shown in Fig. 1 (*b*). The area immediately surrounding the hole is granular in appearance and presumably records the place where the fracture leaped rapidly. Flow markings around cavities may be noted on the surface of 24ST aluminum alloy sheet after pulling beyond the yield point as shown in Fig. 1 (*c*). Such markings are typical of fracture origins in foils and sheet.

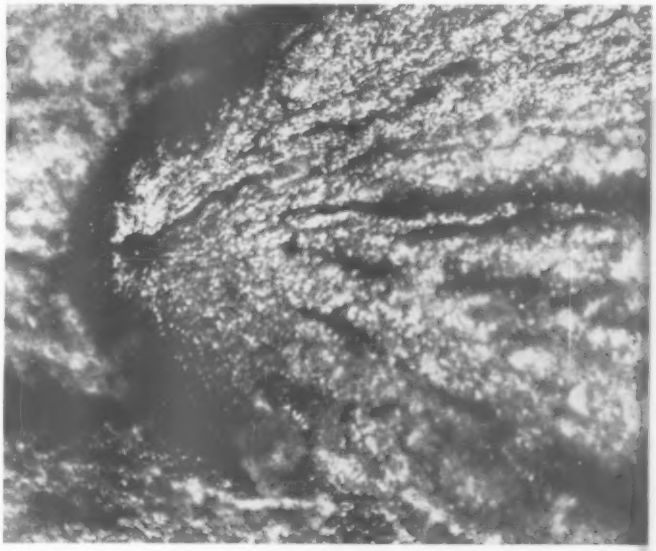


Fig. 1 (a) Fracture surface. 300 ×. Cellulose acetate showing a hole at the initiation of a fracture element

Assignment of the cause of fracture origins to pre-existent flaws may at first seem merely a useless definition in reverse of flaws as those pre-existent local features which give rise to fracture origins. However, fracture origins, like dislocation origins, are conceptually impractical in a perfect lattice. Zener[2] presents a mechanism by which fracture origins may form by accumulation of dislocations, say, at a crystal boundary. Whether or not such a feature might deserve to be called a pre-existent flaw is of no concern here if attention is confined to commercial materials. In addition to occasional pre-existent cracks, these will contain enough local composition segregations, inclusions and poorly bonded regions due to ingot porosity so that more sophisticated mechanisms for fracture origins are unnecessary.

Tear lines follow in the direction of fracture propagation and mark the boundaries between fracture elements spreading forward on different levels. Figure 2 is a photograph of a small group of fracture elements in clear cellulose acetate taken during the course of intermittent fracture propagation. Each element has a hole-like origin and the tear lines are developing at the boundaries between elements. Since fracturing obviously lags behind on

G. R. Irwin is Supt. of the Mechanics Div. and **J. A. Kies** is Metallurgist of the Mechanics Division, with the Naval Research Laboratory.

This paper was presented before the Annual Meeting of the Pressure Vessel Research Committee of the Welding Research Council, New York, N. Y., on Sept. 19, 1951. The opinions expressed in this paper are those of the authors and do not necessarily reflect the views of the U. S. Navy.

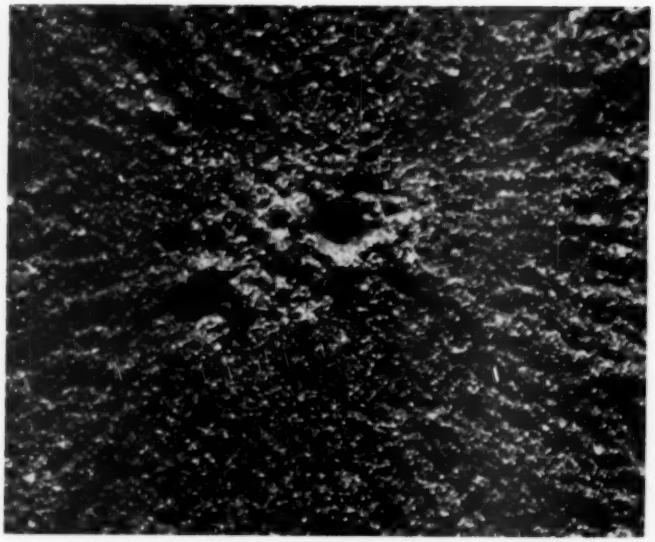


Fig. 1 (b) Fracture surface. 60 ×. Steel tensile bar showing crater-like holes formed at the origin of fracture

Fig. 1 (c) Fracture surface. 650 ×. Surface of 24ST sheet showing holes and accompanying flow markings typical of fracture origins

these lines, the authors and their associates often refer to them as lag lines rather than tear lines. These lag or tear lines and the fracture origins represent the major deformation processes for which energy must be supplied to maintain or increase the velocity of fracturing.

Even in cleavage fractures, the facets reveal complexities attributable to numerous separate initiating elements or "flaws" at which the local volume density of work of deformation must be high. Figure 3 is an example of the river system of tear lines along which fracture elements joined on a cleavage facet of molybdenum. Figure 4 reveals that these tear lines are started where holes or other origins result in new fracture elements on different levels. Such a system of origins can give rise to a field of parabola shaped fracture elements on the cleavage facet of molybdenum as is shown in Fig. 5. Even here each element has a superimposed substructure of finer lines indicating the operation of a wide range of sizes of fracture elements.

A brief descriptive summary of fracturing follows. Fracturing begins at flaws. The early slow stages are accompanied by large amounts of plastic deformation. The progressive extension of a fracture is locally discontinuous. New elements occur on different levels and the joining up of elements at different levels introduces the major local distortions which absorb energy and impede rapid extension of fracturing. The rapid starting and stopping of individual elements of discontinuous fracturing produces mechanical shocks to the adjacent material. This seems adequately to motivate the Neumann lamellas so often seen in close proximity to cleavage fractures in steel and the fan-like patterns of small fracture elements shown in Lucite. Increasing velocity of fracturing is accompanied by a reduction in level differences of new elements of fracture, a simplification of the pattern of lag lines and an approach toward fracturing as a single element on a single plane. This phase, however, is only an intermediate situation because fracturing then requires so little driving energy that the fracture surface will soon be roughened by the forking or branching tendency characteristic of cracks moving at about the velocity of transverse sound waves. During either of the last two stages, the extending severed area may approach a neutral axis where the stress field has no significant component of tension in the direction nominal to the fracture surface. In this event, fracturing stops abruptly. The deformations required for new initiations leave a trace of the hesitation line even though the fracture leaps forward again without measurable delay.

The concept of a limiting velocity for fast fracturing is a natural consequence of the finite propagation velocity for mechanical strain impulses. A theoretical discussion of this point, originally due to Mott, is given in reference 3. Experimentally, the attainment of a limiting velocity for fracturing of centrally notched plates of cellulose acetate has been observed.[4] When fracturing is progressive at constant velocity or nearly so, a mathematical formalism can be applied to obtain the moving strain field which is similar to that which has been applied to obtain strain fields of moving crystalline dislocations.[5] E. H. Yoffe has published a computation of the strain field of a crack dislocation moving at constant velocity.[6] The moving dislocation viewpoint contains some features of considerable interest but the authors feel that their own work and that of others is currently too incomplete to warrant discussion here. In the case of

instability relations governing the approach to rapid fracturing, it is believed there are points of practical importance which should be discussed.

The authors have noted the following from H. C. Boardman's PVRC Fabrication Division Interpretive Report: "Notches, of which cracks are the extreme example, exist in all welded structures due to faulty materials, design or workmanship." Paraphrasing what follows, he says what is needed is conditions such that the cracks, starting at notches, will relieve high local stresses and then stop. Further on, Mr. Boardman says that the engineer is disturbed by the uncertainty of safety factors for welded pressure vessels. The fact that cracks usually stop near where they begin, but sometimes go on to complete failure, indicates that to fail or not to fail can be a touch and go affair such that a seemingly insignificant factor may decide in favor of "go."

In his 1947 lecture at the Metals Congress on Fracturing, Capt. W. P. Roop said, "it is not the structures themselves but the very process of design for strength that is at fault. Something beyond strength is needed." One could quote a series of rephrasings of this comment by others going back at least to 1920 which was the time of publication of the Griffith crack theory and of the size effect experiments by Stanton and Batson.

Fig. 2 260 ×. An intermittently spreading fracture within a cellulose acetate block. Note the lag lines between parabola-like fracture elements

Fig. 3 Cleavage in molybdenum. 350 ×. The lines are caused by level differences between fracture elements. Direction of propagation left to right

Well-controlled size effect studies of the magnitude of the work required to fracture notched steel bars have shown that in the initial stages when the tiny cracks are being formed and are joining up into a definite main crack, the work being done in plastic deformation is proportional to the cube of the dimensions of the test. As the main crack advances into the test piece, this work tends toward proportionality to the area severed. The authors describe this rate of doing work in terms of a quantity, dW/dA, where dW is the work increment and dA the increase in fracture area. Obviously, in a notched bar dW/dA must decrease as the crack penetrates the piece if the work done changes from volume proportionality toward area proportionality.

Thirty years ago, Griffith pointed out that a developing crack in an unbounded tensile stress field would release strain energy in an amount proportional to the three halves power of the fracture area. Assuming that the work done in creating the crack was proportional to the fracture area to the first power, he concluded there must be a critical crack size. Cracks longer than this would develop into fast fractures from energy released in the stress field. This critical crack length decreased with the magnitude of the stress field in the midst of which this crack was imbedded. It increased with the magnitude of dW/dA which he thought of as a surface tension rather than as an irreversible process of plastic flowing. Griffith's reasoning was, of course, sound, assuming validity of his hypotheses. In the practical case of a notched piece of steel with the notch in tension, one readily observes that the conditions present are analogous. However, since the test material is finite in size, there is no assurance that indefinite extension of the crack will lead to instability. The authors call the quantity of strain energy release which accompanies fracturing, dE/dA, where again dA is the increment of fracture area. The critical point will occur when dE/dA exceeds dW/dA and dW/dA is thought of as a decreasing function of crack length.

Let one consider the situation which exists when two notched bar samples of the same steel are bent to an angle, θ, just sufficient for the formation of a starting crack, but not sufficient for appreciable deepening of the fracture. This bend angle will be the same if one sample is four times the size of the other. However, if these sizes and other dimensions of the test have been chosen properly, the situation may be such that with further extension of the crack dE/dA remains much less than dW/dA for the small test bar but be-

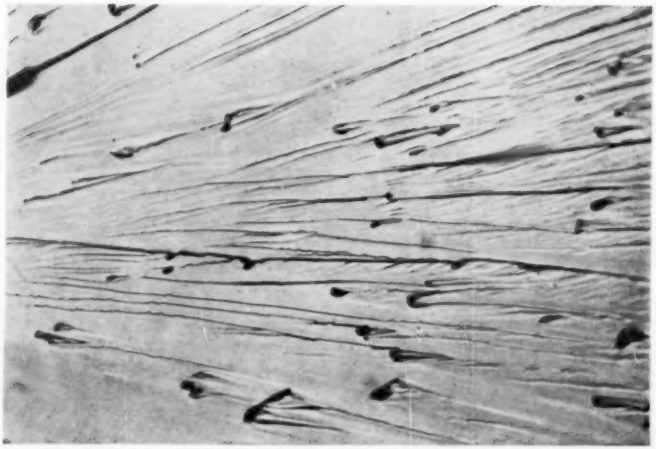


Fig. 4 Cleavage in molybdenum. 350 ×. Discontinuities or flaws serve as starting points for new tear lines between elements on different levels. Propagation left to right

Fig. 5 Cleavage in molybdenum. 175 ×. A field of parabola-like elements starting at what may be holes and bounded by tear lines. Propagation left to right

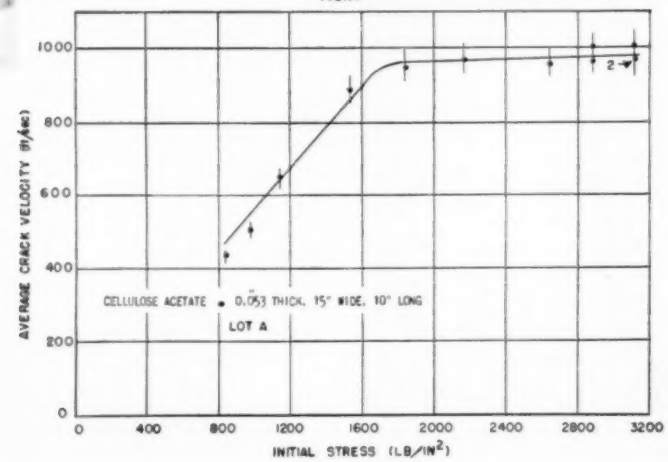


Fig. 6 Effect of stress intensity on the speed of fracture propagation in cellulose acetate sheets (Smith, H. L., and Ferguson, W. J.)

comes comparable to dW/dA for the four-fold larger test bar. Thus, at the initial angle, θ, one has an example of the "go and no go" situation over which Mr. Boardman expressed concern. In this example, the controlling feature is the effect of the size of the test upon dE/dA rather than variation in dW/dA due to material properties. In the large piece, the situation is precarious because even without increase in load the crack may creep in and extend to critical size. It is clear that comparisons containing this "go and no go" situation could be constructed using test pieces of similar size but with one piece containing the risk of instability because of the development in it of smaller values of dW/dA with the deepening of the crack.

A discussion of dW/dA and dE/dA comparisons for a notched bar in simple bending is given in reference 3. The case of a centrally notched plate has been extensively investigated in connection with the brittle fracture of ship plate. This important case lends itself to a study of the role of stored elastic energy in propagating fracture. For simplicity, dA is assumed proportional to dx and dx indicates the increment of motion of the end of the crack. Thus, the work and energy increments dW and dE will be compared for extensions of the crack indicated by dx rather than by dA. It can be shown that, for a given function, dW/dx, the strength should depend on the dimensions of the plate because they partially determine the rate of release of elastic energy, dE/dx, and thus the balance point for instability $dE/dx \geq dW/dx$ can be calculated as a function of x, the crack length and the applied tensile stress.

Greenspan[7], neglecting the lateral restraint by the grips, resolved the Inglis problem so as to readily yield a value for the axial rigidity K factor for a centrally notched plate. For an elliptical hole with the major axis perpendicular to the tensile stress he showed that

$$K = \frac{1}{1 + \dfrac{1 + 2\,(a/b)}{C(n)} \dfrac{V_0}{V_s}} \quad (1)$$

where

a = semimajor axis.
b = semiminor axis.
V_0 = volume of the elliptical hole.
V_s = gross volume of the specimen BLt, where B = width, L = length and t = thickness of the plate.

$$C(n) = \left(1 - \frac{1}{2n^2} - \frac{1}{2n^4}\right)$$

where

$$n = \frac{B}{x} = \frac{B}{2a}.$$

Then setting $y = x/B$ the fraction of the plate width occupied by the hole and letting $b \to 0$ as for a crack,

$$K = \frac{2 - y^2 - y^4}{2 + (r-1)y^2 - y^4} \quad (2)$$

where

$$r = \frac{\pi B}{L}.$$

For sufficiently short cracks y^4 terms may be neglected and

$$K = \frac{2 - y^2}{2 + (r-1)y^2}$$

The stored elastic energy under a given tension is inversely proportional to K, hence if, for example, $y = 0.1$ and $B = L$ as for a square plate $K = K_{(0.1)} \cong 0.98$ and $(1/K_{(0.1)}) \cong 1.02$. The stored elastic energy will be about 2% greater under a given load than in an uncracked plate of the same size. For a given extension under purely elastic conditions the stored elastic energy will conversely be 2% less than in a square plate without a crack. It is important to note that calculations can be readily made only for such cases where contributions to the energy from sources external to the plate are known. For fast fractures and heavy grips, such contributions are generally negligible and boundaries of the plate are essentially fixed.

In order to calculate the rate of release of elastic energy it is necessary to assume some initial load P_0 which would prevail for a given extension e_0 of the plate if the central crack did not exist. $K = 1$ for $x = 0$. For $x > 0$ $K < 1$ and for a plate of thickness t,

$$e_0 = \frac{P_0 L}{YBt} = \frac{PL}{KYBt}$$

where P is any tensile load supported by a plate containing a transverse central crack. The elastic energy is then for plane stress:

$$E_s = \frac{1}{2}\frac{P^2 L}{KYBt} = \frac{1}{2} e_0^2 \frac{YBt}{L} K$$

where

Y = Young's modulus.

Then for fixed ends,

$$\frac{dE_s}{dy} = \frac{1}{2} e_0^2 \frac{YB}{Lt}\frac{dK}{dy}$$

Using the expression for K given in Equation 2, one can write

$$\frac{dE}{dx} = \frac{dE_s}{B\,dy} = -\frac{\pi P_0^2}{BYt} \times \left\{ \frac{y(y^4+2)}{y^8 - 2(r-1)y^6 + (r^2 - 2r - 3)y^4 + 4(r-1)y^2 + 4} \right\} \quad (3)$$

According to the basis on which Greenspan derived K, one should probably not attempt to use this expression for values of $y = x/B > 1/2$. Using the expression for K, one can compute the load supported by the plate assuming elastic conditions to prevail in the presence of a crack for a given extension which would correspond to the load P_0 in the absence of a crack. From this, the average tensile stress normal to the direction of x and in the material beyond the end of the crack is

$$\bar{\sigma} = \frac{\sigma_0 (1+y)(2+y^2)}{2 + (r-1)y^2 - y^4} \quad (4)$$

where σ_0 is the stress corresponding to load P_0 in the uncracked plate. It should be remembered that equilibrium and perfect elasticity are assumed. There is no difficulty in assuming equilibrium or zero particle velocity in the plate material because the situation concerned with here is when instability is just reached. The problem of how to allow for gross plastic flow around the crack requires special treatment beyond the scope of this paper. The above expressions should be confined to cases where plastic flow does not alter the relation $b << a$.

Curves showing plotted values of dE_s/dy are given in Fig. 7 in terms of a rate of release factor to be multiplied by $-\pi P_0^2/Yt$. The curves indicate that a fast ductile fracture may not completely traverse the sheet but may be stopped providing the shape factor r is large. This is equivalent to saying that a sufficiently short extent of stressed material in the direction of the tensile stress may sometimes be adequate provision to prevent total failure. Experiments in the Naval Research Laboratory on long and short aluminum foil specimens have demonstrated this conclusion. For $y < 0.1$ the linear expression used by Griffith coincides with all the curves of Fig. 7. De Leiris[8] has shown that by varying the length of his specimen he can arbitrarily produce either ductile or predominately cleavage fracture in mild steel plate at room temperature.

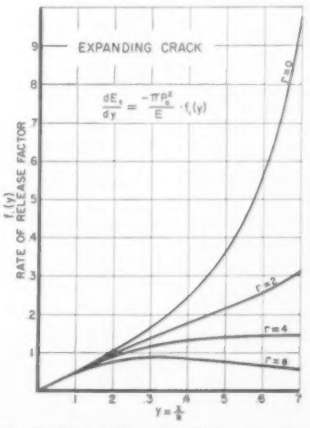


Fig. 7 Plot of Equation 3 showing the dependence on shape factor r of the rate of release of stored elastic energy at various crack lengths for t = unity

Assuming that dW/dx can be relied on to be about 100 in.-lb. per inch of crack length in 1-in. steel under brittle fracture, one can calculate limiting average stresses for extension by strain energy of cracks of given length.

Assuming

$L = 240$ in.
$B = 60$ in.

$$r = \frac{\pi B}{L} = 0.786$$

$y = 0.2$ then f. $(y) = 0.1$
$x = 12$ in.

$$-\frac{dE_s}{dy} \cong \frac{\pi P_0^2}{Yt} \quad (0.1)$$

$Y = 29 \times 10^6$ psi.

Then K from Equation 2 is $0.98B$ and $P_0 = 1.018P$

Since $x/B = y$,

$$\frac{dE_s}{dx} = \frac{1}{B}\frac{dE_s}{dy}$$

Then instability is present for

$$\frac{P^2}{60}\frac{\pi(1.018)^2}{29 \times 10^6}(0.1) \geq 100$$

for $t = 1$ in. or for $P/A \geq 12{,}200$ psi. where A is the gross cross-sectional area of the plate.

It may be noticed that for instability

$$\frac{P}{A} \geq \frac{K\sqrt{\frac{dW}{dA}}}{\sqrt{B}}\sqrt{\frac{Y}{\pi f_1(y)}} \quad (5)$$

For specimens of the same thickness, but whose other dimensions including the slot are in similitude, the allowable stress is inversely proportional to the square root of size as measured by one of the dimensions. For the example above, the dW/dA used was only about one-tenth the work per unit area of a Charpy bar at the 10 ft.-lb. level. This seems realistic because the starting energy must be relatively large in such a test. In the delayed fractures of ships the starting energy may be provided by the effects of time and load reversals under corrosive conditions. A value of 100 in.-lb. per square inch is still well above the surface energy of a metal (about 1×10^{-3} in.-lb. per square inch for a pair of surfaces) and ample allowance remains for deformation near the fracture surface. Under unfavorable conditions such as corrosion fatigue the appropriate dW/dA value could readily become smaller by a factor of 10.

Direct measurements of energy absorption during fast fracture are difficult to obtain and it is suggested that such values of dW/dA be estimated by matching experimentally determined breaking strengths under the severest notch conditions obtainable with computed values of dE_s/dx. A testing machine of great rigidity would simplify the problem. In a relatively soft testing machine such as would prevail with long gripping fixtures of the kind used at Swarthmore College perhaps $K = 0$ would be a sufficiently accurate guess as to the effective shape factor.

An example of such a calculation may be given for specimen $D_N - 33 - 5$ of Table 9 in a report by the Swarthmore group.[9] In this case if r is assumed approximately zero $K = 1$ and $P = P_b$. Then since

$$-\frac{dE_s}{dy} = \frac{\pi P_b^2 f_1(y)}{Yt}$$

the calculation for dE_s/dx in a $^3/_4$-in. plate, 12 in. wide, would be

$$\frac{dE_s}{dx} = -\frac{4\pi P_b^2}{3 \times 12xY} f_1(y)$$

$f_1(y)$ given in Equation 3 is 0.16 for a slot plus crack length of 3.5 in. as was about correct for the specimen. Then $-(dE_s/dx) = 156$ in.-lb. per inch at the time the fracture started to leap. This reduces to 208 in.-lb. per square inch of fracture surface, about one-fifth of the energy per unit area of a V-notch Charpy bar at the 10 ft.-lb. level.

With the aid of the preceding remarks, an engineer can solve all of his pressure vessel safety factor problems with regard to propagation of fracturing. First, he imagines cracks developing wherever cracks are likely to develop in his structure. Then he computes values of dE/dA for lengthening of these cracks under the largest expected loads. Following this, he reviews the test data for his materials to obtain reasonable dW/dA values. If the comparison of this dW/dA value to his possible dE/dA values gives an unsatisfactory safety factor then he must change the material, or the structure, or the allowed maximum loads. It is not known how nearly practical or impractical this procedure is. It is known that the procedure works out as it should for fracture propagation in notched mild steel bars and for fracture propagation in centrally notched plates of various materials. Admittedly, the effects of fatigue, corrosion and strain-aging upon the dW/dA work per unit area values are large and will often make determination of this quantity from experimental data quite difficult. Nevertheless, the governing character of the balance between work done and energy release with extension of the crack must be accepted as basic to any scheme for calculating safety against fracture propagation. It is suggested that one could, in any case, attempt the direct approach presented above in some cases and employ these trials as a guide for collecting additional pertinent information.

In the above, the authors have attempted to present, within certain space limits, a descriptive review of fracturing and fracture dynamics. Because of the importance of progress in design methods to progress in material improvements, a simplified direct application of energy balance principles to calculating the conditions for development of rapid fracturing was discussed and emphasized.

ACKNOWLEDGMENT

The authors are indebted to F. G. Tatnall of the Baldwin Southwark Testing Machine Division of Baldwin-Lima-Hamilton Corp., for his invitation to present this paper at the September 19, 1951, meeting of the Main Committee of the Pressure Vessel Research Committee at New York, and to Miss A. M. Sullivan, Metallurgist, Mechanics Division, Naval Research Laboratory, and to H. L. Smith, Physicist, Mechanics Division, Naval Research Laboratory, who supplied experimental results and photographs used in this presentation.

References

1. Kies, J. A., Sullivan, A. M., and Irwin, G. R., "Interpretation of Fracture Markings," *Jnl. App. Physics*, **21**, 716–720 (1950).
2. Zener, C., *Trans. A.S.M.*, **40A**, 3–31 (1948).
3. Irwin, G. R., "Fracture Dynamics," *Ibid.*, **40A**, 147–166 (1948).
4. Smith, H. L., and Ferguson, W. J., "Crack Velocity Measurements in Cellulose Acetate," Reprint from N. R. L. Progress Reports (April 1950).
5. Eshelby, J. D., *Proc. Phys. Soc. (London)*, **A62**, 307 (1949). Frank, F. C., *Ibid.*, **A62**, 131 (1949). Nabarro, F. R. N., *Ibid.*, **59**, 256 (1947).
6. Yoffe, E. H., "The Moving Griffith Crack," *Phil. Mag.* **42**, 739–750, (July 1951).
7. Greenspan, M., "Axial Rigidity of Perforated Structural Members," *Jnl. Research, Natl. Bureau Standards*, **31**, 305–322 (1943).
8. De Leiris, H., "Sur les Conditions de Production des Dechirures Semi-Fragiles," *Assoc. Tech. Maritime et Aeronautique* (1949).
9. Carpenter, S. T., Roop, W. P., Barr, N. Kasten, E., and Zell, A., "Progress Report on Twelve Inch Flat Plate Tests," Swarthmore College, Serial No. SSG-21 (April 15, 1949).

A Study of the Fatigue Strength of Welded Joints

by W. Soete and R. Van Crombrugge

DESPITE the importance of fatigue tests to designers of welded structures, there is not a great deal of quantitative information on the subject. After a study of a report by Prof. Roš in the January-February 1946 issue of *Schweizer Archiv*, the Welding Committee of the Belgian Standards Institute decided on a program of fatigue tests of butt and fillet welds. It was also decided to determine whether fatigue strength increased with size of weld in the same way as static strength. The Committee on Welded Joints of the Belgian Institute of Welding approved the program, the results of which are contained in this report.

Extended abstract of "Etude de la Resistance a la Fatigue des Assemblages Soudés," published in *Revue de la Soudure*. The tests were performed in the Materials Laboratory of the University of Ghent, Belgium. (Abstracted by Dr. G. E. Claussen.)

The base metal for all tests was steel H.S. 42–50 having the following characteristics:

Tensile strength, 60,000 to 71,000 psi.
Minimum notch impact value before aging, 8.3 mkg./cm.^{2}
Minimum notch impact value after aging, 5.0 mkg./cm.^{2}
Carbon, 0.20% max.
Phosphorus, 0.06% max.
Sulphur, 0.06% max.
Phosphorus + sulphur, 0.11% max.
Silicon, 0.20% max.

All welds were made by the manual arc welding process with alternating current. Butt welds were made in the flat position. Covered electrodes $^1/_8$ and $^5/_{32}$ in. diameter were used at 120 and 140 amp., respectively. The deposited metal had the following properties:

Yield point, 47,000 to 50,000 psi.
Tensile strength, 62,000 to 66,000 psi.
Elongation, 25 to 30% in 5 diameters.
Notch impact value, 12 to 13 mkg./cm.^{2}

All welds were made by a single qualified operator, who understood that he was welding specimens. The welds are believed to be of a quality obtainable in a good shop.

With the exception of one series of tests in which the load was parallel to the welds, all tests were made with load perpendicular to the welds. The tests were made on two Amsler pulsators, one of 30 tons capacity, the other of 100 tons capacity. The 30-ton machine operated at 250 cycles per minute; the 100-ton machine operated at 500 cycles per minute. Tests were discontinued after 2 million cycles. All stresses were tensile, the minimum value in all tests being 1 ton. For practical purposes the tests correspond with zero to maximum tension, similar to the "ursprungsfestigkeit" of Roš and the German investigations. Every test was made without stopping and at room temperature.

BUTT WELDS WITHOUT RESTRAINT

Four series of butt welds were made without restraint:

Series IA, not machined but stress-relieved.
Series IB, machined and then stress-relieved.
Series IIA, as-welded.
Series IIB, machined but not stress-relieved.

The specimens were cut from a plate 0.59 in. thick, 55 x 71 in. with a 60-degree V butt weld down the center parallel to the long dimension. The root of the completed weld was chipped and rewelded. The specimens were 14 in. long, 5½ in. wide. The specimens to be stress-relieved were cut from the central portions of the plate. The specimens without stress relief were cut from the outer portions to assure substantial amounts of residual stress, it was hoped. All cuts were made with a cutting torch. The stress-relieving temperature was 600° C. (1110° F.).

The results are summarized in Table 1. Series IA consisted of five specimens. Fractures occurred at the junction of weld with plate. One specimen broke at a defect in the weld after only 170,000 cycles at 21,300 psi. Series IB consisted of 15 specimens, each of which was ground on both sides. Series IB had 25% higher fatigue strength than IA. Fractures tended to start at undercut which had not been removed by grinding. Series IIA (9 specimens) had about the same fatigue strength as IA, and the fractures were of the same type. Machining improved the fatigue strength of Series IIB (12 specimens) but not to the same extent as IB. Some of the fractures in Series IIB started at blowholes due to damp electrode coatings.

Table 1—Fatigue Strength of Butt Welds Without Restraint

Series	*Fatigue strength, psi.*	*Permissible stress, psi., zero to max. tension*	*Permissible stress, psi., equal tension and compression*
IA, stress-relieved	21,300–23,700	...	...
IB, machined and stress-relieved	28,400–29,800	17,600	12,300
IIA, as-welded	21,300	13,000	9,000
IIB, machined	24,100–25,600	...	...

To draw as much information as possible from our results we have applied two formulas. The first is: $S = B/N^k$ proposed by Committee F on Fatigue Testing of the American Welding Research Council. S is the maximum stress to produce fracture after N cycles (between 50,000 and 4 million), B and K being constants. If F is the stress causing feature after 2 million cycles,

$$F = \frac{B}{(2{,}000{,}000)^k} = \frac{SN^k}{(2{,}000{,}000)^k}$$

or for any number of cycles n between 50,000 and 4,000,000:

$$Fn = S\left(\frac{N}{n}\right)^k$$

This formula may be used to calculate fatigue strength F at a desired number of cycles n from a test result S breaking at N cycles, K being 0.13 for unmachined butt welds and 0.18 for machined butt welds.

The second formula is:

$$S = So\left(1 + K\frac{A}{B}\right)$$

where S is the permissible fatigue stress for a test in which the stress varies from an upper tension B to a lower tension A, and So is the permissible fatigue stress for equal tension and compression. The formula was proposed by Roš. These two formulas were used in Table I.

BUTT WELDS UNDER RESTRAINT

A restrained specimen, Fig. 1, was used having the maximum width that would fit in the testing machine. The butt joint was a double-V weld. The milled longitudinal cuts were prolonged into the head of the specimen, where they terminated in holes 0.08-in. diameter.

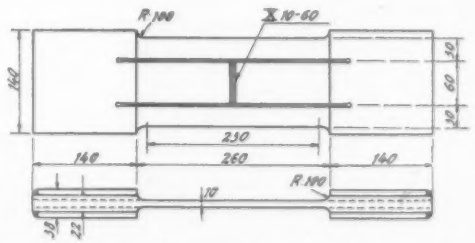


Fig. 1 Restrained butt specimen. Dimension in millimeters

The reinforced ends of the specimen in Fig. 1 were necessary to prevent premature fatigue fracture at the ends of the cuts. Measurements before and after welding revealed compressive stress from 30,000 to 38,000 psi. in the outside columns. Three specimens were tested and gave the following results:

Max. tensile stress, psi.	*No. of cycles*	*Fracture*
19,900	2,197,000	Did not break
22,700	1,832,800	Broke in the weld
22,700	2,131,200	Did not break

It is difficult to avoid the conclusion from these results that residual stresses have a negligible effect on fatigue strength.

Tests were also made on a second type of restrained specimen. A longitudinal groove in the center of each side of a specimen $5^1/_2$ in. wide was filled with weld metal. These specimens contained high residual tensile stress. Three specimens with surfaces machined after welding indicated a fatigue strength of 25,600 psi. Fatigue fractures started at tiny holes in the interior of the weld metal. Three specimens machined and stress-relieved indicated a fatigue strength of over 30,000 psi. Further research seems desirable on the effect of residual stresses on fatigue.

FILLET-WELDED CRUCIFORM SPECIMENS

To determine the fatigue strength of fillet welds as a function of the dimension of the weld, the specimen shown in Fig. 2 was used. Although the specimen has the disadvantage of short welds, it has the advantage of high residual stresses. One series of specimens was tested as welded, the other series was tested after stress relief at 600° C. (1110° F.). The leg of the fillet, d, was 0.20, 0.32, 0.43, 0.51 and 0.71 in. in the first series, and was 0.20, 0.32, 0.47 and 0.59 in. in the second series.

The results for the as-welded specimens showed that the fatigue strength calculated on the area $d \times L$, where L is the length of the weld, was 5700 to 7100 psi. for d = 0.20–0.43 in. Fractures occurred along the junction of the weld with the through plate. For the heavier fillets the fatigue strength calculated on $d \times L$ remained at the same level or even lower, but fracture occurred

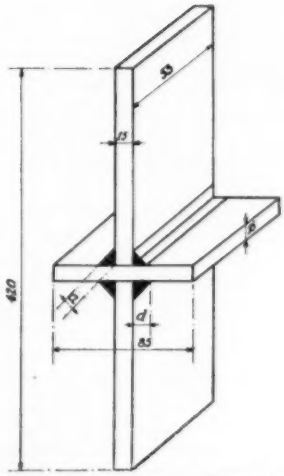


Fig. 2 Cruciform fillet specimen. Dimensions in millimeters

in the grip plate at the toes of the fillets. Based on the plate cross section the fatigue strength for the 0.51-in. fillets was 10,000–11,000 psi; and for the 0.71-in. fillets was 12,000 to 13,500 psi. Practically the same results were obtained with the stress-relieved specimens. The fillet size at which fatigue fracture changes from the through plate to the grip plate is:

$$\frac{2d}{T} = 1.7$$

where T = plate thickness.

Supplementary tests made on specimens with longer welds ($5^1/_2$ in. long) in $^1/_2$ in. plates of mild steel yielded the same value (5700 to 7100 psi.) for the fatigue strength of fillet-welded cruciform specimens. The leg of the fillet in these specimens was 0.43-in., thus $2d/T$ = 1.8 and fractures occurred both in the through plate and of the grip plates. Specimens made of low-alloy steel (minimum tensile strength = 74,000 psi.) had slightly higher fatigue strength: 7500 to 8000 psi. Although no tests were made in which the gap between through plate and grip plates was eliminated by either the use of deep penetration electrodes or beveling the grip plates, tests by Graf and Roš show that cruciform joints with-

Table 2—Results of Fatigue Tests on End Fillet Specimens

Specimen	*Throat of fillet, in.*	*Tensile stress in grip plate, psi.*	*Tensile stress in throat of fillet, psi.*	*No. of cycles*	*Remarks*
1	0.45	15,200	13,000	687,000	Fracture in weld and grip plate
2	0.47	15,200	12,600	840,000	Same
3	0.32	10,000	12,500	1,744,000	Fracture in throat of fillet
4	0.39	10,000	10,000	Over 2 million	No fracture
5	0.41	12,800	10,900	Over 2 million	No fracture
6	0.42	15,200	14,000	361,000	Fracture in weld and grip plate

The values for throat are averages for the four welds.

Table 3—Results of Tests of Side Fillets

Specimen No.	Length of each weld, in.	Width of cover plate, in.	Width of grip plate, in.	Stresses, psi. Cover plate	Stresses, psi. Grip plate	Stresses, psi. Throat	No. of cycles	Fracture
1	6.7	4.9	5.6	13,600	12,000	7,100	330,000	In cover plates
2	6.7	4.7	5.3	23,400	20,700	12,000	74,000	In cover plates
3	3.2	4.9	5.6	13,600	11,900	14,900	94,000	In cover plates
4	3.0	4.9	5.6	4,100	3,500	4,900	>2 million	No fracture
5	2.0	4.4	5.3	5,700	5,400	9,200	>2 million	No fracture
6	2.0	4.5	5.3	7,400	6,400	12,000	946,000	In cover plates
7	2.4	4.7	5.4	7,300	6,400	10,000	>2 million	No fracture
8	2.0	4.7	5.4	8,800	7,600	14,900	744,500	In cover plates
9	2.2	4.7	5.4	7,600	6,800	11,900	1,568,000	In cover plates
10	3.9	4.8	5.4	7,500	6,700	6,400	>2 million	No fracture
11	4.3	4.9	5.5	9,300	7,100	6,400	>2 million	No fracture
12	4.3	4.8	5.5	9,200	8,300	7,200	857,000	In cover plates

out gap have nearly the same fatigue strength as butt welds.

END FILLET WELDS

The specimens with end fillet welds, Fig. 3, were made of mild steel H.S. 37 plates 0.79-in. thick. Each specimen was welded separately without machining or heat treatment. The fillet shape was that of an isosceles right triangle. The specimens were tested in the 100-ton pulsator with a lower tensile stress in the grip plate of 300 psi. The results in Table 2 show that at a fillet size of $2A/T$, where A = throat of fillet, the fracture passes from the throat to the grip plate. End fillets therefore should be so designed. The tests are

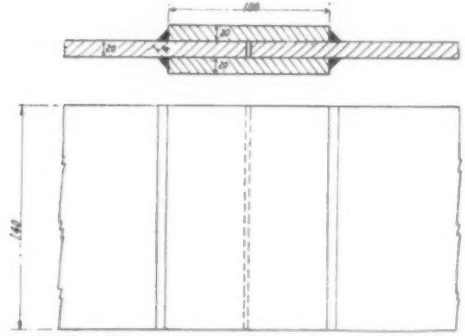


Fig. 3 End fillet specimen. Dimensions in millimeters

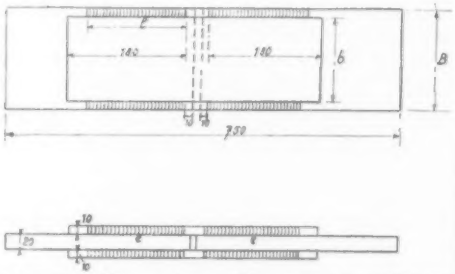


Fig. 4 Side fillet specimen. Dimensions in millimeters

not sufficiently numerous to quote a fatigue strength for end fillets. Fatigue fracture in one specimen started at the overlap of the last bead in the fillet. Once again the importance of depositing a smooth bead was emphasized.

SIDE FILLET WELDS

The specimens with side fillets, Fig. 4, consisted of grip plates 0.79-in. thick and cover plates 0.39 in. thick. The nominal throat of the fillets was 0.31 in. The specimens were neither machined nor heat-treated, and the minimum tensile stress in the cover plates was 1400 psi. It is very difficult to determine an endurance limit from the few test results in Table 3. In all the specimens that fractured, the fracture started in the cover plates a fraction of an inch from the beginning of one of the fillet welds.

Fatigue Tests of Arc-Welded Joints

by H. de Leiris and H. Dutilleul

FATIGUE tests were made on butt and fillet welds in a 50-ton Amsler pulsator at 750 cycles per minute. The stress cycle varied from 2800 psi. tension to maximum tension. Three steels were used having tensile strengths of approximately 57,000 psi., 71,000 psi. and 85,000 psi. Welds were made manually with high-quality electrodes. The fatigue strengths of the unwelded steels were 34,000–37,000 psi., 37,000 psi. and 41,000 psi., respectively.

Double-vee butt welds in $^1/_2$-in. plates of 85,000 psi. steel and single-vee butts in plates of 57,000 psi. steel 0.32 in. thick had a fatigue strength of 17,000 to 23,000 psi. Between 17,000 and 23,000 psi. some specimens failed while others did not in 2 million cycles. The high-tensile steel gave the same results as the low tensile. In these tests the specimens were not machined, the welds were transverse to applied load, and the width of the specimens was about 1.6 in.

Two series of tests were made on single-vee butts in 57,000 psi. steel in which one plate of the butt was thicker than the other. In one series the plates were 0.35 and 0.47 in., in the other series 0.39 and 0.55 in. In both series the welds were made on grooved steel backing strips with 0.20 in. root spacing. The fatigue strengths were the same as if both plates were of the same thickness. Likewise joints with machined bevels had the same fatigue value as joints with torch-cut bevels. The root spacing had no effect on fatigue value, provided there was sufficient opening for good welding. With grooved copper backing strips too wide a root spacing, $^1/_2$ in. for example, lowered the fatigue value. Defects lowered the fatigue value only if they were plainly visible in a radiograph, but transverse cracks lowered the fatigue value seriously. Peening lowered the fatigue value of butt welds to a certain extent.

Three series of tests were made to determine the effect of fillet welding attachments to a plate Fig. 1. In the first series the attachments were 0.32 in. thick, the leg of each fillet being 0.20 in. In the second series the thickness was 0.47 in., the leg 0.28 in. In the third series the thickness was 0.79 in., the leg 0.55 in.

The attachments were transverse to stress. As the thickness of the attachment was increased, the fatigue value fell 20 to 30% below that of butt welds. Fillet welded specimens of the same dimensions as the specimens with welded attachments, Fig. 2, had the same fatigue value.

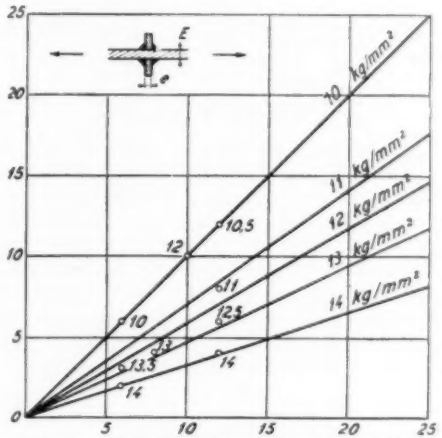


Fig. 1 Fatigue strength of plates with welded attachments transverse to stress

Vertical axis: thickness of attachment
Horizontal axis: thickness of main plate
The numbers on the graph are fatigue strengths in kg./mm.²
10 kg./mm.² = 14,200 psi.
10 mm. = 0.39 in.

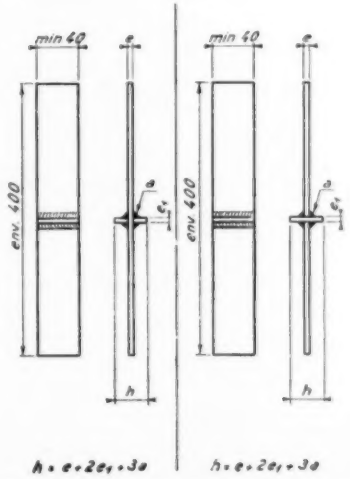


Fig. 2 (Left) Fatigue specimen with welded attachments. (Right) Fatigue specimen of two plates fillet welded to a third plate. Dimensions in millimeters

Abstract of "Essais de fatigue sur assemblages soudés," by H. de Leiris, published in *Soudure et Techniques Connexes*, **5**, 53–65 (1951) and "Les essais de fatigue sur joints soudés à l'arc électrique," by H. Dutilleul published in the same magazine, **5**, 97–108 (1951). The authors are associated with the engineering branch of the French Navy. The tests were performed at the Materials Laboratory of the Technical Service for Naval Construction. The papers were read at meetings of the French Society of Welding Engineers and French Society of Civil Engineers. (Abstracted by Dr. G. E. Claussen.)

The Practical Testing of the Cohesive Strength and Weldability of Steels

Testing methods employed for investigating the suitability of a steel for use in highly stressed welded structures

by W. A. Felix

INTRODUCTION

THE suitability of a steel for use in highly stressed welded structures depends on a number of properties which are not easy to determine in the laboratory. Apart from the yield point and strength as established in the tensile test—the figures on which the designer bases his calculations—the capacity of a steel for deformation under multiaxial stress and its weldability are both important considerations having a bearing on its suitability for the uses in question.

The following examples will give an idea of the testing methods employed for investigating these properties in a large Swiss engineering works.

COHESIVE STRENGTH TESTS

The capacity for deformation of a steel forming part of a welded structure which is subjected to multiaxial stresses is known to depend on the stress conditions, the rate of loading and the temperature. In our opinion the quantitative reproduction of these influences in a laboratory process is not feasible, quite apart from the fact that the stress conditions and the rate of loading are in many cases unknown and cannot be calculated. For this reason we regard the quantitative transference to the design of ductility figures determined in a laboratory test as inadmissible, irrespective of the method used. Recourse must consequently be had to technological tests which permit the qualitative assessment of the tendency to brittle fracture and thus allow the steels to be classified by comparative examination. It is best to select a method which is simple to carry out and demands no large quantities of material, while nevertheless imposing severe requirements. A method of this kind which has long been known and practically applied is the determination of the impact-transition temperature, which is associated with the change from ductile to brittle behavior.[1] As fractures in welded structures tend to begin at sharp internal or external notches, it is advisable to use a specimen with a very sharp notch in order to bring testing conditions as near as possible to the failure conditions prevailing in the actual structure. The transition zone of the steel is then also displaced into temperature ranges which can be very satisfactorily controlled in tests. These considerations led us to employ for our notch impact tests the specimen introduced by Schnadt, in which a sharp notch is produced by knife pressure. This specimen has been described in a previous publication,[2] so that no further account of it need here be given.

In the practical application of the impact tests, the temperatures are first selected so as to bracket the transition zone, whereupon the testing temperature is changed every 5° in the critical range. Alcohol with solid carbonic acid is used as a cold solution. For the evaluation of the curves, the temperatures allocated to the notch impact values of 8, 5 and 2 mkg. per sq. cm. are determined, as shown in Fig. 1. At 8 mkg. per sq. cm. the broken specimens always show a dull, ductile fracture with marked reduction in area. At 5 mkg. per

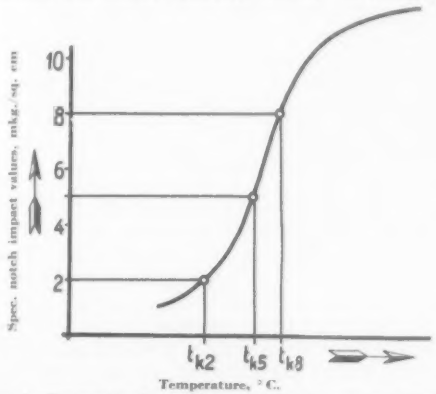


Fig. 1 Determination of the transition temperature in the specific notch impact values and evaluation of the curves obtained

W. A. Felix is Chief Engineer of the Material Testing Dept. of Sulzer Brothers Ltd., Winterhur, Switzerland.

sq. cm., i.e., during the transition from the high figures to the low, the texture of the fracture has in part a brittle, crystalline appearance. X-ray diffraction photographs of the structure of the ruptured surfaces have revealed, however, that in this range the crystallites are still considerably deformed. It is only at 2 mkg. per sq. cm. and less that the interferences in the diffraction photographs of the ruptured surfaces are very little or not at all blurred. In this region brittle fractures practically free from deformation are encountered. Temperature tk_2 therefore appears to be best suited to the comparative assessment of the tendency of a steel to brittle fracture. The assessment of this tendency by the appearance of the ruptured surface fails to give clear and reliable results.

Another point which is important in steel plate intended for welded structures, apart from the tendency to brittle fracture in the condition as supplied, is the tendency to embrittlement after cold deformation. This feature, which is generallly known as sensitivity to aging, can again be investigated by determining, in the manner described above, the position of the abrupt transition in the specific notch impact values of the artificially aged material. The movement of this transition towards higher temperature ranges is then a measure of the sensitivity of the material to aging.

Finally, the capacity of the material for deformation after stress-relief annealing may also be of interest. The same test can again be applied, the material first being submitted to stress-relief annealing at 620° C.

The stress conditions prevailing in a welded structure are to some extent influenced by the thickness of the plates used. The rigidity of the structure and thus the danger of brittle-fracture increase with increasing plate thickness. As the influence of plate thickness is, however, apart from its metallurgical aspects, not accessible to tests on machined specimens, we supplement the notch impact tests by bending tests of welded plate specimens for the purpose of keeping a current check on the tendency to brittle fracture. In this test the plates are used in their full thickness.

The Kommerell bending test of welded-plate specimens was developed in Germany after a number of failures had occurred in welded bridges.[3] The basic idea of this test is to determine the capacity for deformation of the steel under conditions approximating those in the welded and unannealed structure.

For the purposes of the test a single-pass weld bead is made along a plate of the thickness actually used, as shown in Fig. 2. This plate must be sufficiently broad, so that the stresses due to welding exert their full effect and cannot be reduced by warping or distortion. The test plate is now bent with the weld on the tension side, whereupon small cracks appear in the weld metal or in the hardened zone below the seam. As bending proceeds, these cracks extend to the parent metal, a differentiation now manifesting itself between ductile and brittle steels. Brittle steel is unable to hold up the progress of the crack, and the specimen breaks suddenly

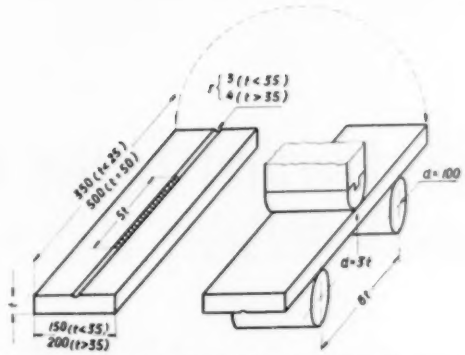
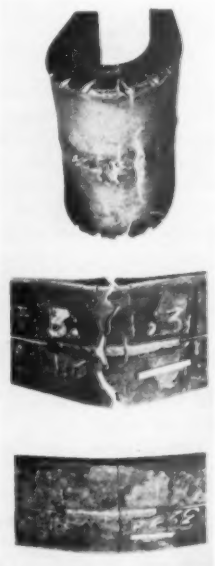


Fig. 2 Bending test of a welded plate after Kommerell

at a small bending angle, showing a bright, crystalline surface of fracture. Ductile steel, on the other hand, stops the advance of the crack by plastic deformation. The specimen can be bent further and will break either not at all or as a result of gradual slip accompanied by pronounced deformation of the cross section.

As the tendency of a steel to brittle fracture is favored by low temperatures as well as by multiaxial stressing, it is quite possible to make the conditions of the welded-plate bending test severer by lowering the test temperature until the point is reached at which fracture without deformation occurs. Fig. 3 illustrates the behavior of a 40-mm. boiler plate of a steel with a tensile strength of 52 kg. per sq. mm. when subjected to the Kommerell



Test temperature +20° C., angle 180 degrees, cracks

Test temperature +0° C., angle 55 degrees, mixed fracture

Test temperature −15° C., angle 22 degrees, brittle fracture

Fig. 3 Bending test of a 40-mm. boiler plate (tensile strength 52 kg. per sq. mm.) at falling temperature

bending test at +20, 0 and −15° C. The determination of the temperature at which brittle fracture begins to occur permits of a further differentiation of the steels to be tested.

WELDABILITY TESTS

The weldability is a further aspect in the assessment of a steel for practical use and has accordingly to be tested also.

According to a recent definition,[4] weldability is a factor determined by the changes which a metal undergoes in the zone affected by heat during the execution of a weld. The main consideration here is the tendency to crack formation. This tendency is doubtless related to hardening and to loss of toughness in the heat-affected zone, and the attempt may be consequently made to assess the weldability of a steel by testing the metal in the heat-affected zone of a single-pass weld bead. The criteria are the maximum hardness and the toughness, which latter can be tested by means of notch-impact specimens whose test cross section runs through the heat-affected zone of the bead, as shown in Fig. 4. The determinant factor is again the temperature range of the abrupt drop in ductility, the displacement of this towards higher temperatures indicating embrittlement during welding.

RESULTS OF PRACTICAL INVESTIGATIONS

The above remarks are illustrated below by results drawn from the practical investigation of five different boiler plates, which were examined with others by the methods here described.

Table I shows the chemical composition of the steels investigated. The tensile, bending and notch impact tests yielded the results shown in Table 2.

Table 1

Steel	*C, %*	*Si, %*	*Mn, %*	*Cr, %*	*Cu, %*	*V, %*
A	0.17	0.40	1.07	..	..	..
B	0.22	0.20	0.99	0.38	0.44	..
C	0.23	0.44	1.37	..	..	..
D	0.21	0.15	1.51	0.12	..	0.18
E	0.18	0.13	1.29	0.72	0.47	Ni 0.21

Table 2

Steel	*Yield point, kg./sq. mm.*	*Tensile strength, kg./sq. mm.*	*Elongation % $l = 5.6\sqrt{F}$*	*Notch toughness, specimen V.S.M. mkg./ sq. cm.*
A	33.0	49.2	32.0	17
B	35.2	57.2	27.3	15
C	41.4	60.8	28.3	19
D	49.1	64.8	26.4	17
E	46.9	64.6	21.9	9

All the steels tested were of high strength. That the inspection of cohesive strength and weldability is of particular importance in such steels has been proved by

Fig. 4 Extraction of impact specimens from the heat-affected zone of a single-pass weld bead

the failures occurring in welded bridges of steel with a tensile strength of 52 kg. per sq. mm. It is remarkable that these steels all have a good capacity for deformation; in the folding test the specimens all allowed of doubling over through 180°, till the legs were touching, without cracks occurring. The notch toughness of the steels, with the exception of Steel E, is also very high. From the point of view of tensile, bending and notch impact strength, the five steels are approximately of equal quality.

Fig. 5 shows the structures of the five steels. Specimens A to D consist of ferrite-pearlite; Steel E has a ferritic-bainitic structure. A striking feature is the extremely fine grain of Steels A, C and D, the structure of which seems to imply special metallurgical treatment during melting.

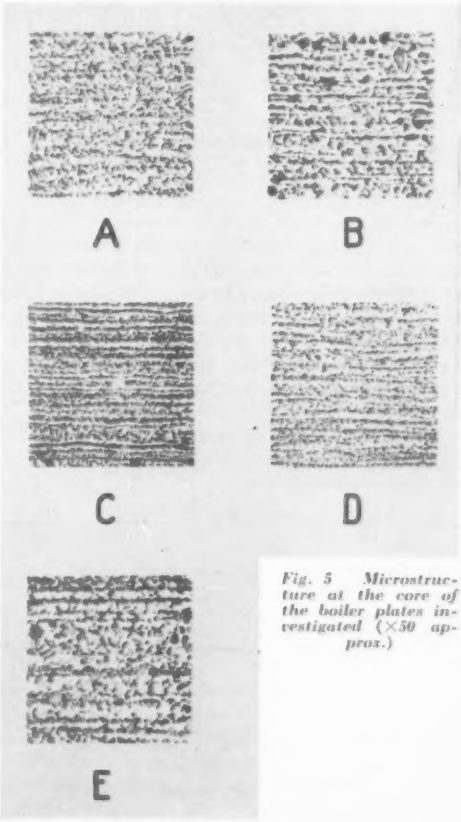


Fig. 5 Microstructure at the core of the boiler plates investigated (×50 approx.)

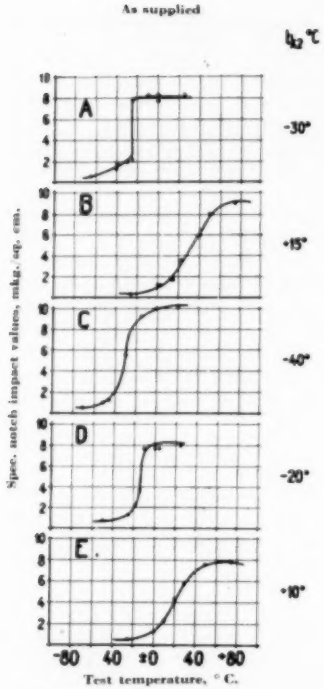


Fig. 6 Steels A–E, transition zone of the specific notch impact values for sharp-notched specimens of the metal as supplied

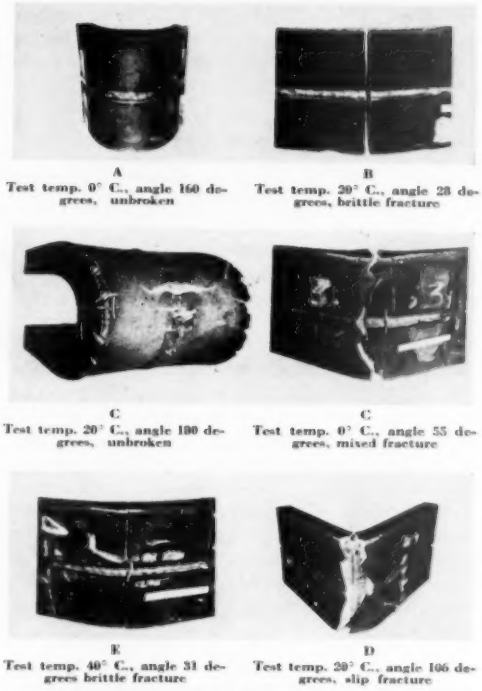

A
Test temp. 0° C., angle 160 degrees, unbroken
B
Test temp. 20° C., angle 28 degrees, brittle fracture
C
Test temp. 20° C., angle 180 degrees, unbroken
C
Test temp. 0° C., angle 55 degrees, mixed fracture
E
Test temp. 40° C., angle 31 degrees brittle fracture
D
Test temp. 20° C., angle 106 degrees, slip fracture

Fig. 7 Steels A–E, results of the welded-plate bending test

While the orthodox tensile, bending and notch impact tests scarcely permitted any distinction between the steels, differences came to light even in the metal as supplied when the position of the transition zone in the specific notch impact values was ascertained. Fig. 6 shows the results of the impact tests with sharp-notched specimens. It is clear that the capacity for deformation of the steels investigated reacts with different sensitivity to the influence of multiaxial stressing.

If ductility is judged by the temperature allocated to a notch impact value of 2 mkg. per sq. cm., the following comparison is obtained:

Steel	t_{k_2}, ° C.
A	−30
B	+15
C	−40
D	−20
E	+ 8

Ductility below 0° C., a temperature which is of special importance for penstock construction, is found under the given test conditions only in Steels A, C and D, the transition zone of which lies at about −10 to −40° C. Steels B and E, with their transition zones in the range of room temperatures, both have low notch impact values at 0° C., that is to say that they are already in the region of pure brittle fractures.

The welded-plate bending test yielded a similar picture (Fig. 7). Its results were as follows:

Steel A, in a plate thickness of 30 mm., was bent through 160° at 0° C. without the cracks formed in the bead penetrating further into the base metal.

Steel B, at the same thickness, suffered sudden brittle fracture over the whole cross section of the specimen at an angle of 30° at room temperature.

Steel C, at 40 mm. thickness, was bent through 180° at room temperature, while at 0° C. it was fractured at an angle of 55°, the appearance of the ruptured surface in part indicating slip and in part brittle fracture.

Steel D, 30 mm. thick, showed a good capacity for deformation at 20° C., an angle of about 100° being reached before the specimen broke completely owing to slip.

Steel E, 40 mm. thick, suffered brittle fracture with a small bending angle at +40° C.

In the light of the results of the welded-plate bending test, Steels A, C and D are again to be regarded as safe from brittle fracture at the given plate thickness and testing temperature, while Steels B and E are subject to brittle fracture.

If the welded-plate bending test is compared with the notch impact values obtained at the same testing temperature (Table 3), some interesting facts are revealed.

Table 3

Steel	*Thickness, mm.*	*Test temp., ° C.*	*Bending test Angle Fracture*	*Notch impact test, mkg./sq. cm. (interpolated)*
A	30	± 0°	160° Unbroken	8 high reg.
B	30	+20°	28° Brittle	3 low reg.
D	30	+20°	106° Slip	8 high reg.
C	40	+20°	180° Unbroken	10 high reg.
C	40	± 0°	55° Mixed	10 high reg.
C	40	−15°	22° Brittle	9 high reg.
C	40	−25°	12° Brittle	8 high reg.
E	40	±40°	31° Brittle	7 high reg.
E	40	± 0°	25° Brittle	1 low reg.

It proves that in the 30-mm. plates the high region of the specific notch impact values corresponds to slip fractures, while the low region corresponds to brittle fractures. This means that at this thickness the metal behaves similarly in the bending test and in the notch impact test.

In the 40-mm. plates the transition from slip fracture to brittle fracture in the welded-plate bending test occurs earlier, with falling temperature, than in the notch impact test. At certain temperatures notch impact values in the high region therefore correspond to brittle fractures in the bending test. It would appear from this that at plate thicknesses of 40 mm. and over the welded-plate bending test imposes severer restrictions on deformation than the test with Schnadt's sharp-notched specimen. As we take great care in making the notches in our specimens, the above fact cannot be attributed to insufficient sharpness of the notch.

The further tests carried out on notch impact specimens were intended to reveal the sensitivity of the five steels to aging. Fig. 8 shows the notch impact curves in the artificially aged condition, the metal having been first 6% drawn down and annealed at 250° C. The displacement of the transition as compared with that of the metal as supplied, which is entered in a lighter line for purposes of comparison, furnishes a measure of the sensitivity of the steel to aging.

Steel A shows practically no sensitivity to aging. *Steel C* very little. Both steels are still in the high region at 0° C. after aging.

Steels B and D, in which the temperature tk_2 moves to considerably higher values, are evidently very sensitive to aging. Steel D, which is sufficiently ductile at 0° C. as supplied, does not offer adequate safety from brittle fracture even at room temperature after having been aged.

Steel E is subject to brittle fracture at room temperature even as supplied, and accordingly even more so in the aged condition. The embrittlement caused by aging is in itself relatively slight.

Finally the stress-relief annealed condition was investigated in the same way. It is not necessary in this case to reproduce the notch impact curves, as they correspond almost exactly to those obtained with the metal as supplied. The sole exception was Steel D (Fig. 9), in which annealing at 620° C. resulted in marked displacement of the transition zone. This is obviously a material which undergoes precipitation hardening at this temperature, with a noticeable reduction of cohesive strength. This embrittlement was in fact revealed in the welded-plate test also. Fig. 10

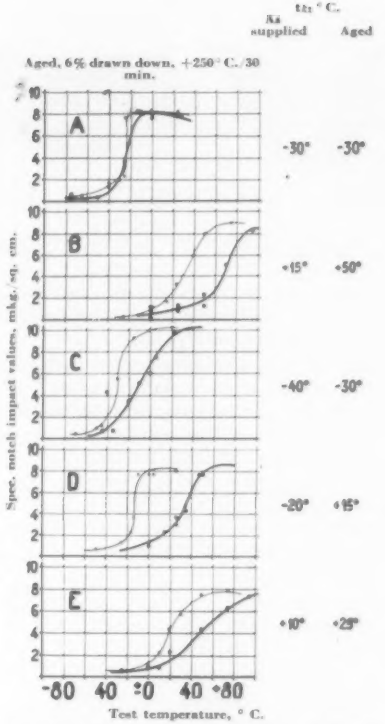


Fig. 8 Steels A–E, transition zone of the specific notch impact values as supplied (light curve) and after artificial aging (heavy curve)

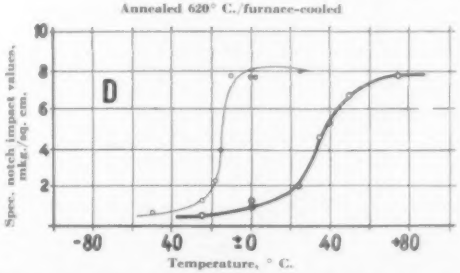


Fig. 9 Steel D, transition zone of metal as supplied (light curve) and after stress-relief annealing

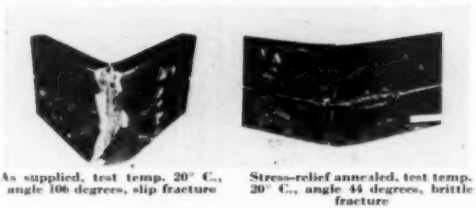

Steel D
As supplied, test temp. 20° C., angle 106 degrees, slip fracture
Stress-relief annealed, test temp. 20° C., angle 44 degrees, brittle fracture

Fig. 10 Steel D, behavior in the welded-plate bending test as supplied and after stress-relief annealing

shows the curves for the bending specimens as supplied and after stress-relief annealing. The difference in the capacity for deformation is plain.

Finally the results obtained in the weldability tests may be quoted; in these hardening and embrittlement in the heat-affected zone of a single-pass weld bead were investigated in the manner described above. Fig. 11 shows the transition in the specific notch impact values as compared with that for the metal supplied, together with the maximum hardness measurements. The following assessment of weldability was arrived at on the basis of the results obtained:

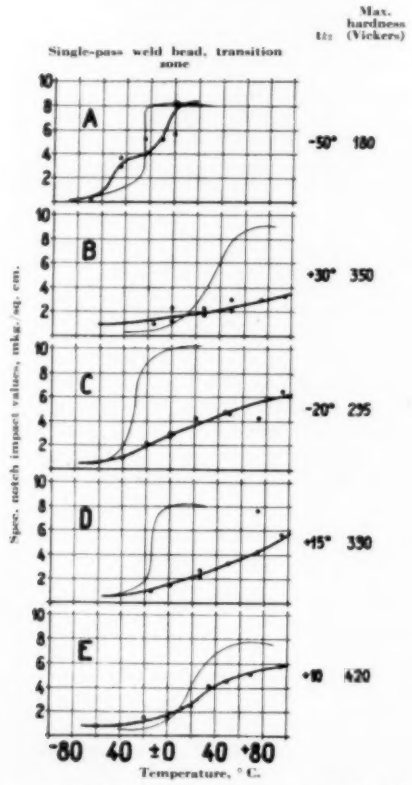


Fig. 11 Steels A–E, transition zone as supplied (light curve) and in the heat-affected zone of a single-pass weld bead (heavy curve)

Steel A may be classified as unaffected by welding. It is only slightly hardened by a single-pass weld bead and retains its full cohesive strength in the heat-affected zone.

Steel B is very sensitive to welding. Hardening is pronounced, and to judge by the flat course of the notch impact curve the metal is also very sensitive to cracking.

Steel C is of sufficient toughness at room temperature in spite of the noticeable hardening. Weldability must be rated as satisfactory to good.

Steels D and E must be regarded, on the evidence of hardening and the run of the notch impact curve, as sensitive to welding, Steel E being somewhat tougher than Steel D in spite of the more pronounced hardening in the heat-affected zone.

If the results of all the investigations are collated (Table 4), the general suitability of the steels for use in highly stressed welded structures may be summed up as follows:

Table 4

—Test of:— Steel	Tensile strength, kg./sq. mm.	Ductility: As supplies, tk_2	Ductility: Aged, tk_2	Ductility: Annealed 620° C., tk_2	—Weldability— Single-pass bead, max. hardness (Vickers)	—Weldability— Weld, tk_2
A	49.2	−30°	−30°	−35°	180	−50°
B	57.2	+15°	+50°	+15°	350	+30°
C	60.8	−40°	−30°	−35°	295	−20°
D	64.8	−20°	+15°	+23°	390	+15°
E	64.6	+10°	+25°	± 0°	420	+10°

Steel A, with a tensile strength of 50 kg. per sq. mm., is safe from brittle fracture down to a temperature of −20° C., and its properties are not impaired either by aging or by annealing at 620° C. The metal is not sensitive to welding, as the hardening in the heat-affected zone of a single-pass weld is only slight and there is no embrittlement. These qualities make this steel extremely suitable for highly stressed welded structures; preheating before welding may be dispensed with, as may also, under suitable conditions, the subsequent heat treatment of the welds.

Steel B, with a strength of 57 kg. per sq. mm., is susceptible to brittle fracture even at room temperature under severe conditions. The steel is highly sensitive to aging and welding and must therefore be classified as completely unsuitable for welded structures.

Steel C, with a strength of 61 kg. per sq. mm., is safe from brittle fracture as supplied and in the aged condition down to a temperature of 0° C. Owing to its high strength it tends to marked hardening and to a certain degree of welding embrittlement. While it is quite suitable for welding, it is best preheated when the plate thickness exceeds 30 mm., while the finished welds should be stress-relief annealed.

Steel D, with a strength of 65 kg. per sq. mm., is safe from brittle fracture as supplied, but loses this property to a large extent after stress-relief annealing on account

of embrittlement caused by precipitation hardening. The metal is in addition sensitive to aging and welding and thus unsuitable for our purposes.

Steel E, with a strength of 65 kg. per sq. mm. is not fully proof against brittle fractures at 0° C. as supplied. Annealing at 620° C. somewhat improves its cohesive strength. The metal is however, noticeably sensitive to aging and to some extent to welding, so that it is unsuitable for thick-walled welded structures.

The considerable differences thus revealed in the five steels investigated seem to us remarkable by comparison with the results obtained in the orthodox tensile, bending and notch impact tests. We feel, therefore, that the testing of cohesive strength and weldability on the lines indicated above is a procedure of undeniable interest to those engaged in the selection and investigation of metals intended for use in highly stressed welded structures.

References

[1] Vanderbeck, R. W., and Gensamer, M., The Welding Journal, 29 (1), 37–48 (1950).
[2] Felix, W. A., *Sulzer Tech. Rev.*, No. 4, (1950).
[3] *Stahl u. Eisen*, **57**, 421 (1937).
[4] Jackson, C. E., and Luther, G. G., The Welding Journal, 19 (10) Research Suppl., 351-s–364-s (1940).

Announcement of Predoctoral Fellowships on the Fundamental Sciences Related to Welding

IT IS the purpose of these fellowships to provide young men who have demonstrated marked ability an opportunity to conduct basic research in the applied sciences related to welding and its closely allied fields. These fellowships are supported by the Chicago Bridge & Iron Co., International Harvester Co., The Linde Air Products Co. and others. Any University desiring to apply for one of these fellowships should indicate the type of problem which the University wishes to undertake, the name of the faculty adviser, and a biographical sketch and complete information in regard to the Fellows as indicated later on in this brochure.

QUALIFICATIONS OF APPLICANTS

A Fellow must be a citizen of the United States who has demonstrated ability and aptitude for advanced work, and who has training in welding, or in the fundamental sciences related to welding equivalent to at least a Bachelor's Degree in a university of recognized standing.

FIELDS OF STUDY

It is expected that these Fellows will work on scientific problems related to welding, or in the fundamental sciences related to welding.

FELLOWSHIP ACTIVITIES

A Fellow will be required to devote his entire time to advanced study and research, except that he may, when approved in advance by the Board, do a limited amount of teaching at an appropriate level as a part of his training. A Fellow may not receive aid from another fellowship, scholarship or similar grant during the tenure of the fellowship unless approved by the Board.

CONDITIONS OF APPOINTMENTS

The fellowships will be awarded at a regular meeting of the Fellowship Board.

To receive consideration for tenure during any academic year, applications must be filed on or before May 1st preceding the academic year. Awards will be announced about July 1st preceding the academic year.

A candidate for a fellowship shall be nominated by an institution in the United States which is prepared to furnish equipment and supervision suitable for the types of problem mentioned under "Fields of Study." Nominations for a fellowship grant should be forwarded by a responsible official of the institution concerned and must be accompanied by complete college transcript, photograph of the proposed Fellow and a plan for study and research. In the event that it is not possible to lay out a detailed program for study at the time of application, a general indication should be given of the method and procedure that will be followed. This should be supplemented with a definite plan two months after the beginning of the academic year. The plan should include the name of the research adviser who will guide the course of the research work.

The proposed Fellow may be requested to appear before the Board, or its designated representative, for a personal interview. In such cases, travel expenses will be paid by Welding Research Council.

The initial appointment shall be for a one- or a two-year period. Reappointment in either case for an additional year may be made upon application when warranted by the progress and accomplishments of the Fellow. A fellowship appointment is subject to the condition that neither the general plan for study and research, nor the faculty adviser shall be changed without the consent of the Board. Unless otherwise arranged, tenure will begin in September.

A fellowship does not carry any commitment on the part of the Fellow, or the supporting Companies, with regard to employment.

Administered by the Welding Research Council in the name of Chicago Bridge & Iron Co., International Harvester Co., The Linde Air Products Co. and other companies.

GRANTS

The annual fellowship stipend, which will be determined in every case by the Fellowship Board, will be a maximum of $1800 for a single man, and $2500 for a married man, except as noted below. An added amount not to exceed $1000, may be provided annually to the institution to which each Fellow is assigned for tuition, special fees, travel or necessary supplies. In the case of successful applicants who are eligible for benefits under Public Laws 346 or 16, the fellowship award will be supplementary to such benefits and will carry an adjusted stipend to be determined by the Board.

LOCATION OF WORK

The fellowships will be awarded to individuals for study and research in this country. The faculty adviser shall be approved by the Board.

REPORTS

The Research Fellow shall submit semiannual progress reports on January 30th and June 30th. Upon the termination of a fellowship, each Fellow will be required to submit to the Board a detailed report of the principal results of his research and fellowship experience.

PUBLICATIONS

It is understood that the results of work carried on by a Fellow, whether in the form of a doctoral thesis or not, shall be available to the public through accepted scientific channels without restriction. Each publication of results of an investigation made during the tenure of the fellowship should include a statement indicating that the research was conducted while the author was a Chicago Bridge & Iron Co., International Harvester Co., The Linde Air Products Co. or other Research Fellow in welding under the Welding Research Council. If publications resulting from work under a fellowship appear the author is expected to furnish 20 copies of each paper to the Fellowship Board of the Welding Research Council.

APPLICATIONS

Applications or requests for additional information, should be addressed to the Welding Research Council, 29 W. 39th St., New York 18, N. Y.

Members of the Fellowship Board:

John P. Magos, *Chairman:* Director of Research Engineering and Research Division, Crane Co., 4100 S. Kedzie Ave., Chicago 5, Ill.

C. A. Adams, *Ex officio:* 417 W. Price St., Philadelphia 44, Pa.

H. C. Boardman, Director of Research, Chicago Bridge & Iron Co., 1305 W. 105th St., Chicago 43, Ill.

W. F. Hess, Head, Department of Metallurgical Engineering, Rensselaer Polytechnic Institute, Troy, N. Y.

W. B. Kouwenhoven, Dean, School of Engineering, Johns Hopkins University, Baltimore, Md.

J. T. Norton, Department of Metallurgy, Massachusetts Institute of Technology, Cambridge, Mass.

H. B. Rose, Assistant Director of Manufacturing, International Harvester Co., 180 N. Michigan Ave., Chicago 1, Ill.

R. D. Stout, Department of Metallurgical Engineering, Lehigh University, Bethlehem, Pa.

David Swan, Metallurgical Engineer, Union Carbide and Carbon Research Labs., Inc., 30 E. 42nd St., New York 17, N. Y.

NOW AVAILABLE

"PHYSICAL AND MECHANICAL CHARACTERISTICS OF MATERIALS FOR PRESSURE VESSELS"

AN ANNOTATED BIBLIOGRAPHY

Volume I: Introduction and Subject Index: *Volume II:* Abstracts of the Literature Published from 1937 through 1938 (Including some earlier references); *Volume III:* Abstracts of the Literature Published from 1939 through 1943; *Volume IV:* Abstracts of the Literature Published from 1944 through 1947.

Approximately 2500 "dittoed" papers—"Accopress" Binders

Price Per Set: $30.00

Address All Orders to:

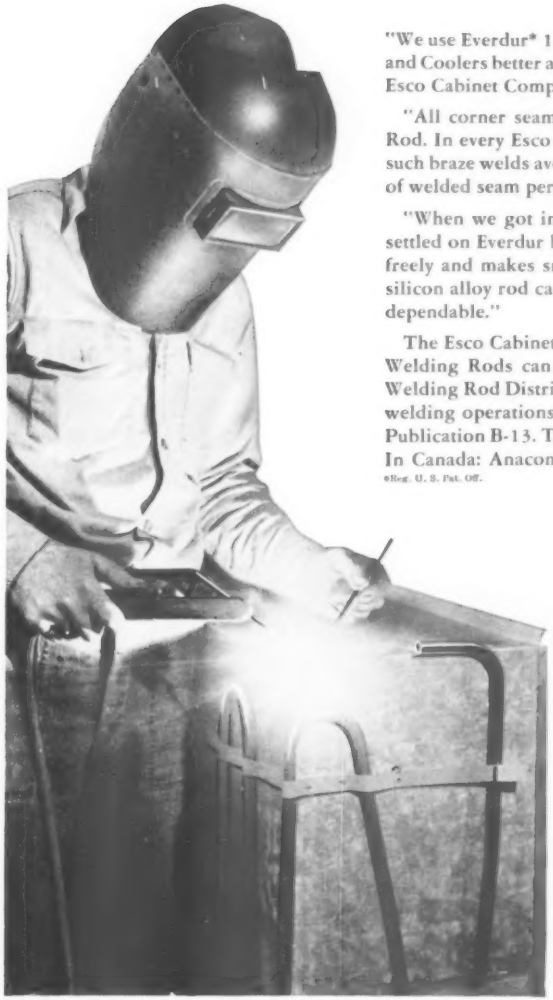
Pressure Vessel Research Committee of the Welding Research Council

29 West Thirty-Ninth Street Room 503 New York 18, New York

LIMITED EDITION, ONLY FIRST 50 ORDERS WILL BE FILLED

"We braze-weld corner seams at the rate of 6 to 10 inches per minute with EVERDUR WELDING ROD

—welds are easy to grind—and never rust"



"We use Everdur* 1010 Welding Rod to help build our ESCO Freezers and Coolers better and faster," says Mr. H. R. Smithson, Chief Engineer, Esco Cabinet Company, West Chester, Pa.

"All corner seams in all our units are braze-welded with Everdur Rod. In every Esco frozen food cabinet, for instance, we make twelve such braze welds averaging 1 ½ inches each at the rate of 6 to 10 inches of welded seam per minute, depending on the gap between the joints.

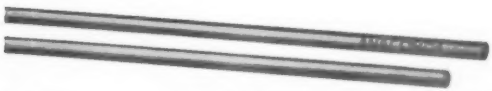
"When we got into this work, we tried various welding rods and settled on Everdur because it has a relatively low melting point, flows freely and makes smooth seams that are easy to finish. This copper-silicon alloy rod can't rust—it has plenty of strength and is uniformly dependable."

The Esco Cabinet Co.'s experience is typical of the way ANACONDA Welding Rods can cut production welding costs. Your ANACONDA Welding Rod Distributor can suggest the right rod for your brazing or welding operations. Ask him, and write to us today for your copy of Publication B-13. The American Brass Company, Waterbury 20, Conn. In Canada: Anaconda American Brass Ltd., New Toronto, Ontario.

*Reg. U. S. Pat. Off. 61163

ESCO operator using Everdur 1010 Rod to braze-weld corner seams of milk-cooling cabinet. Metal is 18-gage galvanized steel.

ESCO operator using ⅛" Everdur 1010 Welding Rod to braze-weld corner seams in frozen food cabinet liner. Metal welded is 20-gage plain and galvanized steel.



braze or weld with confidence . . . use

ANACONDA®
WELDING RODS

if...

— You cut steel to irregular shapes of all kinds . . . circles . . . squares . . . rectangles . . . straight lines with square or beveled edges . . .

— You demand precision, production line steel shape-cutting . . .

— You work with steel plates, slabs, billets, or forgings . . . in metal up to 6-inches or more in thickness . . .

Then pick up your fountain pen and fill in the coupon below for all the information contained in the easy-reading 12-page booklet describing the many features and advantages of the production-proven new Airco No. 50 Travograph. Please mail the coupon to us today, or write us on your business letterhead.

AIRCO

AIR REDUCTION

AIR REDUCTION SALES COMPANY • AIR REDUCTION MAGNOLIA COMPANY
AIR REDUCTION PACIFIC COMPANY
REPRESENTED INTERNATIONALLY BY AIRCO COMPANY INTERNATIONAL
Divisions of Air Reduction Company, Incorporated
Dealers and Offices in Principal Cities

AT THE FRONTIERS OF PROGRESS YOU'LL FIND

AN AIR REDUCTION PRODUCT

Air Reduction Sales Company
Division of Air Reduction Company, Inc.
Advertising Department
60 East 42nd Street
New York 17, N. Y.

Gentlemen:

Tell me about the No. 50 Travograph today. Send me your catalog.

Name _____ **Title** _____

Company _____

Address _____

City _____ **Zone** _____ **State** _____